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Chapter 16

INDUCTION MOTOR DESIGN ABOVE 100KW AND CONSTANT V/f

16.1 INTRODUCTION

Induction motors above 100 kW are built for low voltage (480 V/50 Hz, 460 V/60 Hz, 690V/50Hz) or higher voltages, 2.4 kV to 6 kV and 12 kV in special cases.

The advent of power electronic converters, especially those using IGBTs, caused the increase of power/unit limit for low voltage IMs, 400V/50Hz to 690V/60Hz, to more than 2 MW. Although we are interested here in constant V/f fed IMs, this trend has to be observed.

High voltage, for given power, means lower cross section easier to wind stator windings. It also means lower cross section feeding cables. However, it means thicker insulation in slots, etc. and thus a low slot-fill factor; and a slightly larger size machine. Also, a high voltage power switch tends to be costly. Insulated coils are used. Radial – axial cooling is typical, so radial ventilation channels are provided. In contrast, low voltage IMs above 100 kW are easy to build, especially with round conductor coils (a few conductors in parallel with copper diameter below 3.0 mm) and, as power goes up, with more than one current path, $a_1 > 1$. This is feasible when the number of poles increases with power: for $2p_1 = 6, 8, 10, 12$. If $2p_1 = 2, 4$ as power goes up, the current goes up and preformed coils made of stranded rectangular conductors, eventually with 1 to 2 turns/coil only, are required. Rigid coils are used and slot insulation is provided.

Axial cooling, finned-frame, unistack configuration low-voltage IMs have been recently introduced up to 2.2 MW for low voltages (690V/60Hz and less).

Most IMs are built with cage rotors but, for heavy starting or limited speed-control applications, wound rotors are used.

To cover most of these practical cases, we will unfold a design methodology treating the case of the same machine with: high voltage stator and a low voltage stator, and deep bar cage rotor, double cage rotor, and wound rotor, respectively.

The electromagnetic design algorithm is similar to that applied below 100 kW. However the slot shape and stator coil shape, insulation arrangements, and parameters expressions accounting for saturation and skin effect are slightly, or more, different with the three types of rotors.

Knowledge in Chapters 9 and 11 on skin and saturation effects, respectively, and for stray losses is directly applied throughout the design algorithm.

The deep bar and double-cage rotors will be designed based on fulfilment of breakdown torque and starting torque and current, to reduce drastically the number of iterations required. Even when optimization design is completed, the latter will be much less time consuming, as the “initial” design is meeting approximately the main constraints. Unusually high breakdown/rated torque ratios ($t_{be} = T_{bk}/T_{en} > 2.5$) are to be approached with open stator slots and larger l_i/τ ratios to obtain low stator leakage inductance values.

$$T_{bk} \approx \frac{3p_l}{2} \left(\frac{V_{ph}}{\omega_l} \right)^2 \frac{1}{L_{sc}}; \quad L_{sc} = L_{ls} + L_{lr} \quad (16.1)$$

where L_{sl} is the stator leakage and L_{lr} is the rotor leakage inductance at breakdown torque. It may be argued that, in reality, the current at breakdown torque is rather large ($I_k/I_{ln} \geq T_{bk}/T_{en}$) and thus both leakage flux paths saturate notably and, consequently, both leakage inductances are somewhat reduced by 10 to 15%. While this is true, it only means that ignoring the phenomenon in (16.1) will yield conservative (safe) results.

The starting torque T_{LR} and current I_{LR} are

$$T_{LR} \approx \frac{3(R_r)_{S=1} K_{istart}^2 I_{LR}^2 p_l}{\omega_l} \quad (16.2)$$

$$I_{LR} \approx \frac{V_{lph}}{\sqrt{(R_s + (R_r)_{S=1})^2 + \omega_l^2 ((L_{sl})_{S=1} + (L_{rl})_{S=1})^2}} \quad (16.3)$$

In general, $K_{istart} = 0.9 - 0.975$ for powers above 100 kW. Once the stator design, based on rated performance requirements, is done, with R_s and L_{sl} known, Equations (16.1) through (16.3) yield unique values for $(R_r)_{S=1}$, $(L_{rl})_{S=1}^{sat}$ and $(L_{rl})_{S=S_n}$. For a targeted efficiency with the stator design done and core loss calculated, the rotor resistance at rated power (slip) may be calculated approximately,

$$(R_r)_{S=S_n} = \left(\frac{P_n}{\eta_n} - 3R_s I_{ln}^2 - p_{iron} - p_{stray} - p_{mec} \right) \frac{1}{3(K_i I_{ln})^2} \quad (16.4)$$

with

$$K_i = \frac{(I_r)_{S=S_n}}{I_{ln}} \approx 0.8 \cos \varphi_{ln} + 0.2; \quad I_{ln} = \frac{P_n}{\sqrt{3} V_{ln} \cos \varphi_{ln} \eta_n} \quad (16.5)$$

We may assume that rotor bar resistance and leakage inductance at $S = 1$ represent 0.80 to 0.95 of their values calculated from (16.1 through 16.4).

$$(R_{be})_{S=1} = (0.85 - 0.95) \frac{(R_r)_{S=1}}{K_{bs}}; K_{bs} = \frac{4m(W_l K_{wl})^2}{N_r} \quad (16.6)$$

$$(L_{be})_{S=1} = (0.75 - 0.80) \frac{(L_{rl})_{S=1}^{\text{sat}}}{K_{bs}} \quad (16.7)$$

Their values for rated slip are

$$(R_{be})_{S=S_n} = (0.7 - 0.85) \frac{(R_r)_{S=S_n}}{K_{bs}} \quad (16.8)$$

$$(L_{be})_{S=S_n} = (0.8 - 0.85) \frac{(L_{rl})_{S=S_n}}{K_{bs}} \quad (16.9)$$

With rectangular semiclosed rotor slots, the skin effect K_R and K_x coefficients are

$$K_R = \frac{(R_{be})_{S=1}}{(R_{be})_{S=S_n}} \approx \xi = \beta_{\text{skin}} h_r; \beta_{\text{skin}} = \sqrt{\frac{\pi f_i \mu_0}{\rho_{Al}}} \quad (16.10)$$

$$\frac{(L_{be})_{S=1}^{\text{unsat}}}{(L_{be})_{S=S_n}} \approx \frac{\frac{h_r}{3b_r} K_x + \frac{h_{or}}{b'_{or}}}{\frac{h_r}{3b_r} + \frac{h_{or}}{b_{or}}} \quad (16.11)$$

Apparently, by assigning a value for h_{or}/b_{or} , Equation (16.11) allows us to calculate b_r because

$$K_x \approx \frac{3}{2\beta_{\text{skin}} h_r} \quad (16.12)$$

Now the bar cross section for given rotor current density j_{AL} , ($A_b = h_r \cdot b_r$) is

$$A_b = \frac{I_b}{j_{Al}} = \frac{K_i I_{ln}}{K_{bi} j_{AL}}; K_{bi} = \frac{2m_l W_l K_{wl}}{N_r} \quad (16.13)$$

If A_b from (16.13) is too far away from $h_r \cdot b_r$, a more complex than rectangular slot shape is to be looked for to satisfy the values of K_R and K_x calculated from (16.10 and 16.11).

It should be noted that the rotor leakage inductance has also a differential component which has not been considered in (16.9) and (16.11).

Consequently, the above rationale is merely a basis for a closer-to-target rotor design from the point of view of breakdown, starting torques, and starting current.

A similar approach may be taken for the double cage rotor, but to separate the effects of the two cages, the starting and rated power conditions are taken to design the starting and working cage, respectively.

16.2 HIGH VOLTAGE STATOR DESIGN

To save space, the design methodology will be unfolded simultaneously with a numerical example of an IM with the following specifications:

- $P_n = 736 \text{ kW (1000HP)}$
- Targeted efficiency: 0.96
- $V_{ln} = 4 \text{ kV } (\Delta)$
- $f_1 = 60 \text{ Hz}$, $2p_1 = 4 \text{ poles}$, $m = 3 \text{ phases}$;

Service: Si_1 continuous, insulation class F, temperature rise for class B (maximum 80 K).

The rotor will be designed separately for three cases: deep bar cage, double cage, and wound rotor configurations.

➤ Main stator dimensions

As we are going to again use Esson's constant (Chapter 14), we need the apparent airgap power S_{gap} .

$$S_{gap} = 3EI_{ln} = 3K_E V_{lph} I_{ln} \quad (16.14)$$

$$\text{with} \quad K_E = 0.98 - 0.005 \cdot p_1 = 0.98 - 0.005 \cdot 2 = 0.97. \quad (16.15)$$

The rated current I_{ln} is

$$I_{ln} = \frac{P_n}{\sqrt{3} V_{ln} \cos \varphi_n \eta_n} \quad (16.16)$$

To find I_{ln} , we need to assign target values to rated efficiency η_n and power factor $\cos \varphi_n$, based on past experience and design objectives.

Although the design literature uses graphs of η_n , $\cos \varphi_n$ versus power and number of pole pairs p_1 , continuous progress in materials and technologies makes the η_n graphs quickly obsolete. However, the power factor data tend to be less dependent on material properties and more dependent on airgap/pole pitch ratio and on the leakage/magnetization inductance ratio (L_{sc}/L_m) as

$$(\cos \varphi)_{\text{zero loss}}^{\text{max}} \approx \frac{1 - \frac{L_{sc}}{L_m}}{1 + \frac{L_{sc}}{L_m}} \quad (16.17)$$

Because L_{sc}/L_m ratio increases with the number of poles, the power factor decreases with the number of poles increasing. Also, as the power goes up, the ratio L_{sc}/L_m goes down, for given $2p_1$ and $\cos \varphi_n$ increases with power.

Furthermore, for high breakdown torque, L_{sc} has to be small as the maximum power factor increases. Adopting a rated power factor is not easy. Data of Figure 16.1 are to be taken as purely orientative.

Corroborating (16.1) with (16.17), for given breakdown torque, the maximum ideal power factor $(\cos\phi)_{\max}$ may be obtained.

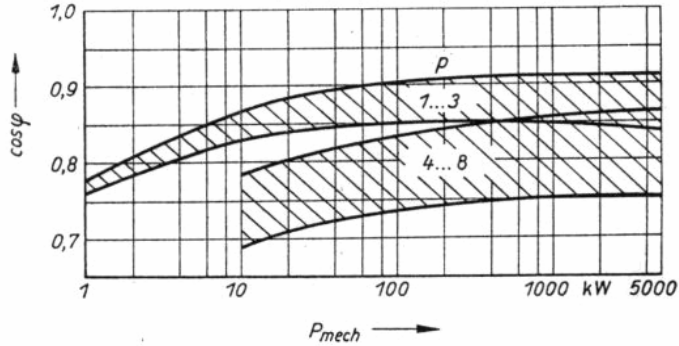


Figure 16.1 Typical power factor of cage rotor IMs

For our case $\cos\phi_n = 0.92 - 0.93$.

Rated efficiency may be purely assigned a desired, though realistic, value. Higher values are typical for high efficiency motors. However, for $2p_1 < 8$, and $P_n > 100$ kW the efficiency is above 0.9 and goes up to more than 0.95 for $P_n > 2000$ kW. For high efficiency motors, efficiency at 2000 kW goes as high as 0.98 with recent designs.

With $\eta_n = 0.96$ and $\cos\phi_n = 0.92$, the rated phase current I_{Inf} (16.16) is

$$I_{\text{Inf}} = \frac{736 \cdot 10^{-3}}{3 \cdot 4 \cdot 10^3 \cdot 0.92 \cdot 0.96} = \frac{120.42}{\sqrt{3}} \text{ A}$$

From (16.14), the airgap apparent power S_{gap} becomes

$$S_{\text{gap}} = \sqrt{3} \cdot 0.97 \cdot 4000 \cdot 120.42 = 808.307 \cdot 10^3 \text{ VA}$$

➤ Stator main dimensions

The stator bore diameter D_{is} may be determined from Equation (15.1) of Chapter 15, making use of Esson's constant,

$$D_{\text{is}} = \sqrt[3]{\frac{2p_1 P_1 S_{\text{gap}}}{\pi \lambda_1 f_1 C_0}} \quad (16.18)$$

From Figure 14.14 (Chapter 14), $C_0 = 265 \cdot 10^3 \text{ J/m}^3$, $\lambda = 1.1$ = stack length/pole pitch (Table 15.1, Chapter 15) with (16.18), D_{is} is

$$D_{is} = \sqrt[3]{\frac{2 \cdot 2}{\pi \cdot 1.1} \frac{2}{60} \frac{808.307 \cdot 10^3}{265 \cdot 10^3}} = 0.4\text{m}$$

The airgap is chosen at $g = 1.5 \cdot 10^{-3}$ m as a compromise between mechanical constraints and limitation of surface and tooth flux pulsation core losses.

The stack length l_i is

$$l_i = \lambda \tau = \lambda \cdot \frac{\pi D_{is}}{2p_1} = 1.1 \frac{\pi \cdot 0.49}{2 \cdot 2} = 0.423\text{m} \quad (16.19)$$

➤ Core construction

Traditionally the core is divided between a few elementary ones with radial ventilation channels between. Such a configuration is typical for radial-axial cooling (Figure 16.2). [1]

Complete range of enclosures and cooling arrangements can be built from the same basic design by using modular construction.

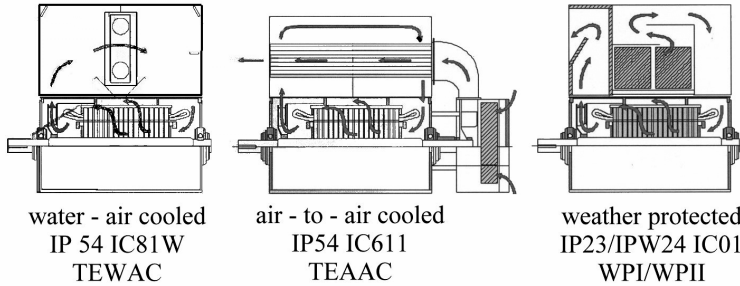


Figure 16.2 Divided core with radial-axial air cooling (source ABB)

Recently the unistack core concept, rather standard for low power (below 100 kW), has been extended up to more than 2000 kW both for high and low voltage stator IMs. In this case axial aircooling of the finned motor frame is provided by a ventilator on the motor shaft, outside bearings (Figure 16.3). [2]

As both concepts are in use and as, in Chapter 15, the unistack case has been considered, the divided stack configuration will be considered here for a high voltage stator case.

The outer/inner stator diameter ratio intervals have been recommended in Chapter 15, Table 15.2. For $2p_1 = 4$, let us consider $K_D = 0.63$.

Consequently, the outer stator diameter D_{out} is

$$D_{out} = \frac{D_{is}}{K_D} = \frac{0.49}{0.63} = 0.777\text{m} \approx 780\text{mm} \quad (16.20)$$

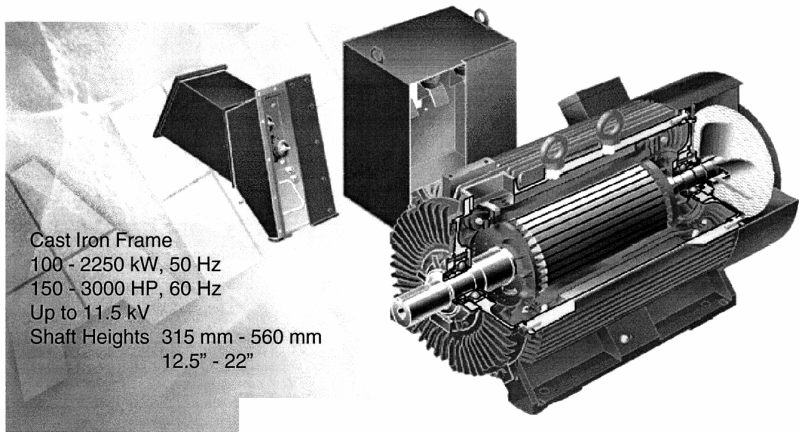


Figure 16.3 Unistack with axial air cooling (source, ABB)

The airgap flux density is taken as $B_g = 0.8$ T. From Equation (14.14) (Chapter 14), C_0 is

$$C_0 = K_B \alpha_i K_{w1} \pi A_1 B_g 2p_1 \quad (16.21)$$

Assuming a tooth saturation factor $(1 + K_{st}) = 1.25$, from Figure 14.13 Chapter 14, $K_B = 1.1$, $\alpha_i = 0.69$. The winding factor is given a value $K_{w1} \approx 0.925$. With $B_g = 0.8$ T, $2p_1 = 4$, and $C_0 = 265 \cdot 10^3 \text{ J/m}^3$, the stator rated current sheet A_1 is

$$A_1 = \frac{265 \cdot 10^3}{1.1 \cdot 0.69 \cdot 0.925 \cdot \pi \cdot 0.8 \cdot 4} = 37.565 \cdot 10^3 \text{ Aturns/m}$$

This is a moderate value.

The pole flux ϕ is

$$\phi = \alpha_i \tau l_1 B_g; \quad \tau = \frac{\pi D_{is}}{2p_1} \quad (16.22)$$

$$\tau = \frac{\pi \cdot 0.49}{2 \cdot 2} = 0.314 \text{ m}; \quad \phi = 0.69 \cdot 0.314 \cdot 0.423 \cdot 0.8 = 0.0733 \text{ Wb}$$

The number of turns per phase W_1 ($a_1 = 1$ current paths) is

$$W_1 = \frac{K_E V_{ph}}{4 K_B f_1 K_{w1} \phi} = \frac{0.97 \cdot 4000}{4 \cdot 1.1 \cdot 60 \cdot 0.925 \cdot 0.0733} = 207.8 \quad (16.23)$$

The number of conductors per slot n_s is written as

$$n_s = \frac{2m_1 a_1 W_1}{N_s} \quad (16.24)$$

The number of stator slots, N_s , for $2p_1 = 4$ and $q = 6$, becomes

$$N_s = 2p_1 q_1 m_1 = 2 \cdot 2 \cdot 6 \cdot 3 = 72 \quad (16.25)$$

So

$$n_s = \frac{2 \cdot 3 \cdot 1 \cdot 207.8}{72} = 17.31$$

We choose $n_s = 18$ conductors/slot, but we have to decrease the ideal stack length l_i to

$$l_i = l_i \cdot \frac{17.31}{18} = 0.423 \cdot \frac{17.31}{18} \approx 0.406\text{m}$$

$$\text{The flux per pole } \phi = \phi \cdot \frac{18}{17.31} = 0.07049\text{W}$$

The airgap flux density remains unchanged ($B_g = 0.8$ T).

As the ideal stack length l_i is final (provided the teeth saturation factor K_{st} is confirmed later on), the former may be divided into a few parts.

Let us consider $n_{ch} = 6$ radial channels, each 10^{-2}m wide ($b_{ch} = 10^{-2}\text{m}$). Due to axial flux fringing its equivalent width $b_{ch}' \approx 0.75b_{ch} = 7.5 \cdot 10^{-3}\text{m}$ ($g = 1.5$ mm). So the total geometrical length L_{geo} is

$$L_{geo} = l_i + n_{ch} b_{ch}' = 0.406 + 6 \cdot 0.0075 = 0.451\text{m} \quad (16.26)$$

On the other hand, the length of each elementary stack is

$$l_s = \frac{L_{geo} - n_{ch} b_{ch}}{n_{ch} + 1} = \frac{0.451 - 6 \cdot 0.01}{6 + 1} \approx 0.056\text{m} \quad (16.27)$$

As lamination are 0.5 mm thick, the number of laminations required to make l_s is easy to match. So there are 7 stacks each 56 mm long (axially).

➤ The stator winding

For high voltage IMs, the winding is made of form-wound (rigid) coils. The slots are open in the stator so that the coils may be introduced in slots after prefabrication (Figure 16.4). The number of slots per pole/phase q_1 is to be chosen rather large as the slots are open and the airgap is only $g = 1.5 \cdot 10^{-3}\text{m}$.

The stator slot pitch τ_s is

$$\tau_s = \frac{\pi D_{is}}{N_s} = \frac{\pi \cdot 0.49}{72} = 0.02137\text{m} \quad (16.28)$$

The coil throw is taken as $y/\tau = 15/18 = 5/6$ ($q_1 = 6$). There are 18 slots per pole to reduce drastically the 5th mmf space harmonic.

-- MICADUR - Compact Industry Insulation System (MCI)

-- voidless insulation construction

-- tight fit in the slots

-- free of partial discharges

-- high thermal conductivity

-- good moisture resistance

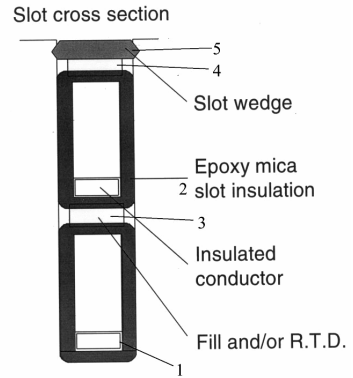


Figure 16.4 Open stator slot for high voltage winding with form-wound (rigid) coils

The winding factor K_{w1} is

$$K_{w1} = \frac{\sin \frac{\pi}{6}}{6 \cdot \sin \frac{\pi}{6}} \sin \frac{\pi}{2} \frac{5}{6} = 0.9235$$

The winding is fully symmetric with $N_s/m_1 a_1 = 24$ (integer), $2p_1/a_1 = 4/1$ (integer). Also, $t = \text{g.c.d}(N_s, p_1) = p_1 = 2$, and $N_s/m_1 t = 72/(3 \cdot 2) = 12$ (integer).

The conductor cross section A_{Co} is (delta connection)

$$A_{Co} = \frac{I_{Inf}}{a_1 J_{Co}}; a_1 = 1, J_{Co} = 6.3 \text{ A/mm}^2; I_{Inf} = 69.36 \quad (16.29)$$

$$A_{Co} = \frac{120.42}{1 \cdot 6.3 \sqrt{3}} = 11.048 \text{ mm}^2 = a_c \cdot b_c$$

A rectangular cross section conductor will be used. The rectangular slot width b_s is

$$b_s = \tau_s \cdot (0.36 \div 0.5) = 0.021375 \cdot (0.36 \div 0.5) = 7.7 \div 10.7 \text{ mm} \quad (16.30)$$

Before choosing the slot width, it is useful to discuss the various insulation layers (Table 16.1).

The available conductor width in slot a_c is

$$a_c = b_s - b_{ins} = 10.0 - 4.4 = 5.6 \text{ mm} \quad (16.31)$$

Table 16.1 Stator slot insulation at 4kV

Figure 16.4	Denomination	thickness (mm)	
		tangential	radial
1	conductor insulation (both sides)	$1.04 = 0.4$	$18.04 = 7.2$
2	epoxy mica coil and slot insulation	4	$4.2 = 8.0$
3	interlayer insulation	-	$2.1 = 2$
4	wedge	-	$1.4 = 4$
Total		$b_{ins} = 4.4$	$h_{ins} = 21.2$

This is a standardised value and it was considered when adopting $b_s = 10$ mm (16.30). From (16.19), the conductor height b_c becomes

$$b_c = \frac{A_{Co}}{a_c} = \frac{11.048}{5.6} \approx 2\text{mm} \quad (16.32)$$

So the conductor size is 2×5.6 mm×mm.

The slot height h_s is written as

$$h_s = h_{ins} + n_s b_c = 21.2 + 18 \cdot 2 = 57.2\text{mm} \quad (16.33)$$

Now the back iron radial thickness h_{cs} is

$$h_{cs} = \frac{D_{out} - D_{is}}{2} - h_s = \frac{780 - 490}{2} - 57.2 = 87.8\text{mm} \quad (16.34)$$

The back iron flux density B_{cs} is

$$B_{cs} = \frac{\phi}{2l_i h_{cs}} = \frac{0.07049}{2 \cdot 0.406 \cdot 0.0878} = 0.988\text{T} \quad (16.35)$$

This value is too small so we may reduce the outer diameter to a lower value: $D_{out} = 730$ mm; the back core flux density will now be close to 1.4T.

The maximum tooth flux density B_{tmax} is:

$$B_{tmax} = \frac{\tau_s B_g}{\tau_s - b_s} = \frac{21.37 \cdot 0.8}{21.37 - 10} = 1.5\text{T} \quad (16.36)$$

This is acceptable though even higher values (up to 1.8 T) are used as the tooth gets wider and the average tooth flux density will be notably lower than B_{tmax} .

The stator design is now complete but it is not definitive. After the rotor is designed, performance is computed. Design iterations may be required, at least to converge K_{st} (teeth saturation factor), if not for observing various constraints (related to performance or temperature rise).

16.3 LOW VOLTAGE STATOR DESIGN

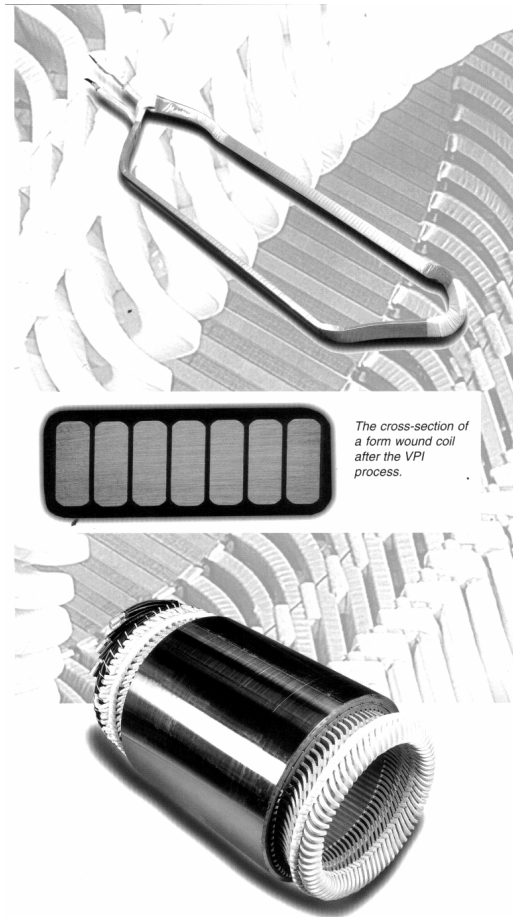


Figure 16.5 Open slot, low voltage, single-stack stator winding (axial cooling) – (source, ABB)

Traditionally, low voltage stator IMs above 100kW have been built with round conductors (a few in parallel) in cases where the number of poles is large so that many current paths in parallel are feasible ($a_1 = p_1$).

Recent extension of variable speed IM drives tends to lead to the conclusion that low voltage IMs up to 2000 kW and more at 690V/60Hz (660V, 50Hz) or at (460V/50Hz, 400V/50Hz) are to be designed for constant V and f , but having in view the possibility of being used in general purpose variable speed drives with voltage source PWM IGBT converter supplies. To this end, the machine insulation is enforced by using almost exclusively form-wound coils and open

stator slots as for high voltage IMs. Also insulated bearings are used to reduce bearing stray currents from PWM converters (at switching frequency).

Low voltage PWM converters are a costly advantage. Also, a single stator stack is used (Figure 16.5).

The form-wound (rigid) coils (Figure 16.5) have a small number of turns (conductors) and a kind of crude transposition occurs in the end-connections zone to reduce the skin effect. For large powers and $2p_1 = 2, 4$, even $2 - 3$ elementary conductors in parallel and $a_1 = 2, 4$ current paths may be used to keep the elementary conductors within a size with limited skin effect (Chapter 9).

In any case, skin effect calculations are required as the power goes up and the conductor cross section follows path. For a few elementary conductors or current path in parallel, additional (circulating current) losses occur as detailed in Chapter 9 (paragraphs 9.2 and 9.3).

Aside from these small differences, the stator design follows the same path as high voltage stators.

This is why it will not be further treated here.

16.4 DEEP BAR CAGE ROTOR DESIGN

We will now resume the design methodology in paragraph 16.2 with the deep bar cage rotor design. More design specifications are needed for the deep bar cage.

$$\begin{aligned}\frac{\text{breakdown torque}}{\text{rated torque}} &= \frac{T_{bk}}{T_{en}} = 2.7 \\ \frac{\text{starting current}}{\text{rated current}} &= \frac{I_{LR}}{I_n} \leq 6.1 \\ \frac{\text{starting torque}}{\text{rated torque}} &= \frac{T_{LR}}{T_{en}} = 1.2\end{aligned}$$

The above data are merely an example.

As shown in paragraph 16.1, in order to size the deep bar cage, stator leakage reactance X_{sl} is required. As the stator design is done, X_{sl} may be calculated.

➤ Stator leakage reactance X_{sl}

As documented in Chapter 9, the stator leakage reactance X_{sl} may be written as

$$X_{sl} = 15.8 \left(\frac{f_1}{100} \right) \left(\frac{W_l}{100} \right)^2 \frac{l_i}{p_1 q_1} \sum \lambda_{is} \quad (16.37)$$

where $\sum \lambda_{is}$ is the sum of the leakage slot (λ_{ss}), differential (λ_{ds}), and end connection (λ_{fs}) geometrical permeance coefficients.

$$\lambda_{ss} = \frac{(h_{s1} - h_{s3})}{3b_s} K_\beta + \frac{h_{s2}}{b_s} K_\beta + \frac{h_{s3}}{4b_s} \quad (16.38)$$

$$K_\beta = \frac{1+3\beta}{4}; \beta = \frac{y}{\tau} = \frac{5}{6}$$

In our case, (see [Table 16.1](#) and [Figure 16.6](#)).

$$h_{s1} = n_s \cdot b_c + n_s \cdot 0.4 + 2 \cdot 2 + 1 = 18 \cdot 2 + 18 \cdot 0.4 + 2 \cdot 2 + 1 = 48.2 \text{ mm} \quad (16.39)$$

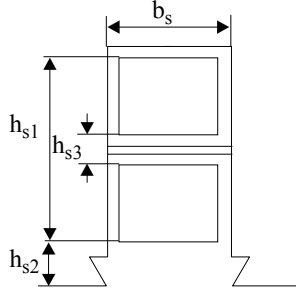


Figure 16.6 Stator slot geometry

Also, $h_{s3} = 2 \cdot 2 + 1 = 5 \text{ mm}$, $h_{s2} = 1 \cdot 2 + 4 = 6 \text{ mm}$. From (16.37),

$$\lambda_{ss} = \left(\frac{48.2 - 5}{3 \cdot 10} + \frac{6}{10} \right) \left(\frac{1 + 3 \frac{5}{6}}{4} \right) + \frac{5}{4 \cdot 10} = 1.91$$

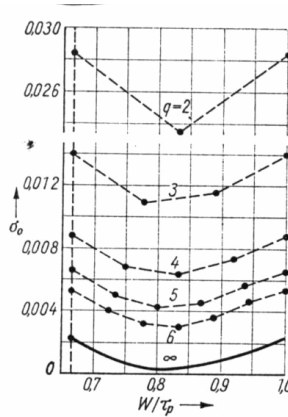


Figure 16.7 Differential leakage coefficient.

The differential geometrical permeance coefficient λ_{ds} is calculated as in Chapter 6.

$$\lambda_{ds} = \frac{0.9 \cdot \tau_s (q_l K_{w1})^2 K_{01} \sigma_{d1}}{K_c g} \quad (16.40)$$

with
$$K_{01} \approx 1 - 0.033 \frac{b_s^2}{g \tau_s} = 1 - 0.033 \frac{10^2}{1.5 \cdot 21.37} = 0.8975 \quad (16.41)$$

σ_{d1} is the ratio between the differential leakage and the main inductance, which is a function of coil chording (in slot pitch units) and q_s (slot/pole/phase) – [Figure 16.7](#): $\sigma_{d1} = 0.3 \cdot 10^{-2}$.

The Carter coefficient K_c (as in Chapter 15, Equations 15.53–15.56) is

$$K_c = K_{c1} K_{c2} \quad (16.42)$$

K_{c2} is not known yet but, as the rotor slot is semiclosed, $K_{c2} < 1.1$ with $K_{c1} \gg K_{c2}$ due to the fact that the stator has open slots,

$$\gamma_1 = \frac{b_s^2}{5g + b_s} = \frac{10^2}{5 \cdot 1.5 + 10} = 5.714; \quad K_{c1} = \frac{\tau_s}{\tau_s - \gamma_1} = \frac{21.37}{21.37 - 5.71} = 1.365 \quad (16.43)$$

Consequently, $K_c \approx 1.365 \cdot 1.1 = 1.50$.

From (16.40),

$$\lambda_{ds} = \frac{0.9 \cdot 21.37 (6 \cdot 0.923)^2 0.895 \cdot 0.3 \cdot 10^{-2}}{1.5 \cdot 1.5} = 0.6335$$

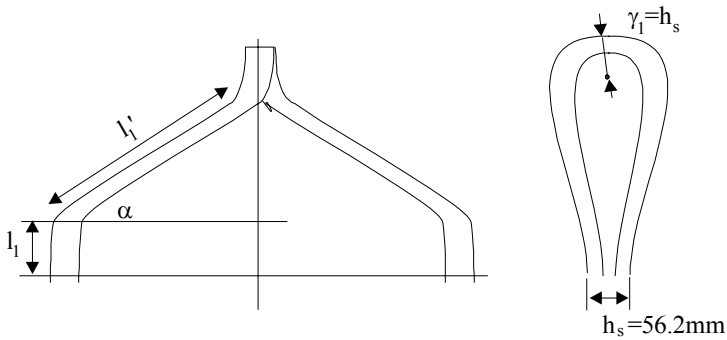


Figure 16.8 Stator end connection coil geometry

The end-connection permeance coefficient λ_{fs} is

$$\lambda_{fs} = 0.34 \frac{q_l}{l_i} (l_{fs} - 0.64 \beta \tau) \quad (16.44)$$

l_{fs} is the end connection length (per one side of stator) and may be calculated based on the end connection geometry in [Figure 16.8](#).

$$\begin{aligned} l_{fs} &\approx 2(l_1 + l_1') + \pi\gamma_1 = 2\left(l_1 + \frac{\beta\tau}{\sin\alpha}\right) + \pi h_s = \\ &= 2\left(0.015 + \frac{5}{6} \frac{1}{2} \frac{0.314}{\sin 40^\circ}\right) + \pi \cdot 0.0562 = 0.548\text{m} \end{aligned} \quad (16.45)$$

So, from (16.44),

$$\lambda_{ts} = 0.34 \frac{6}{0.406} \left(0.548 - 0.64 \frac{5}{6} 0.314\right) = 1.912$$

Finally, from (16.37), the stator leakage reactance X_{ls} (unaffected by leakage saturation) is

$$X_{ls} = 15.8 \left(\frac{60}{100}\right) \left(\frac{2 \cdot 6 \cdot 18}{100}\right)^2 \frac{0.406}{2 \cdot 6} (1.91 + 0.6335 + 1.912) = 6.667\Omega$$

As the stator slots are open, leakage flux saturation does not occur even for $S = 1$ (standstill), at rated voltage. The leakage inductance of the field in the radial channels has been neglected.

The stator resistance R_s is

$$\begin{aligned} R_s &= K_R \rho_{\text{Co80}^\circ} \frac{W_1^2}{A_{\text{Co}}} (L_{\text{geo}} + l_{fs}) = \\ &= 1 \cdot 1.8 \cdot 10^{-3} \left(1 + \frac{80 - 20}{272}\right) \cdot 12 \cdot 18 \cdot \frac{2 \cdot (0.451 + 0.548)}{11.048 \cdot 10^{-6}} = 0.8576\Omega \end{aligned} \quad (16.46)$$

Although the rotor resistance at rated slip may be approximated from (16.4), it is easier to compare it to stator resistance,

$$(R_r)_{S=S_n} = (0.7 \div 0.8) R_s = 0.8 \cdot 0.8576 = 0.686\Omega \quad (16.47)$$

for aluminium bar cage rotors and high efficiency motors. The ratio of $0.7 - 0.8$ in (16.47) is only orientative to produce a practical design start. Copper bars may be used when very high efficiency is targeted for single-stack axially-ventilated configurations.

➤ The rotor leakage inductance L_{rl} may be computed from the breakdown torque's expression (16.1)

$$L_{rl} = \frac{3p_1}{2T_{LR}} \left(\frac{V_{1ph}}{\omega_1}\right)^2 - L_{sl}; \quad L_{sl} = \frac{X_{sl}}{\omega_1} = \frac{6.667}{2\pi 60} = 0.0177\text{H} \quad (16.48)$$

with

$$T_{LR} = t_{LR} T_{en} = t_{LR} \frac{P_n}{\omega_l} p_l \quad (16.49)$$

Now, L_{rl} is

$$L_{rl} = \frac{3p_l V_{lph}^2}{2t_{LR} P_n \omega_l} - L_{sl} = \frac{3 \cdot 4000^2}{2 \cdot 2.7 \cdot 736 \cdot 10^3 \cdot 2\pi 60} - 0.0177 = 0.01435H \quad (16.50)$$

From starting current and torque expressions (16.2 and 16.3),

$$(R_r)_{S=1} = \frac{\omega_l t_{LR} P_n \left(\frac{\omega_l}{p_l} \right)^{-1}}{3p_l K_{istart} I_{LRphase}^2} = \frac{1.2 \cdot 736 \cdot 10^3}{3 \cdot 0.975^2 \cdot \left(5.6 \frac{120}{\sqrt{3}} \right)^2} = 2.0538\Omega \quad (16.51)$$

$$\begin{aligned} (L_{rl})_{S=1} &= \frac{1}{\omega_l} \sqrt{\left(\frac{V_{lph}}{I_{LRphase}} \right)^2 - (R_s + (R_r)_{S=1})^2} - (L_{sl})_{S=1} = \\ &= \frac{1}{2\pi 60} \left[\sqrt{\left(\frac{4000}{5.6 \frac{120}{\sqrt{3}}} \right)^2 - (1.083 + 2.0537)^2} - 6.667 \right] = 8.436 \cdot 10^{-3} H \quad (16.52) \end{aligned}$$

Note that due to skin effect $(R_r)_{S=1} = 2.914(R_s)_{S=S_n}$ and to both leakage saturation and skin effect, the rotor leakage inductance at stall is $(L_{rl})_{S=1} = 0.5878(L_{rl})_{S=S_n}$.

Making use of (16.8) and (16.10), the skin effect resistance ratio K_R is

$$K_R = \frac{(R_r)_{S=1}}{(R_r)_{S=S_n}} \cdot \frac{0.95}{0.8} = 2.999 \cdot \frac{0.95}{0.8} = 3.449 \quad (16.53)$$

The deep rotor bars are typically rectangular, but other shapes are also feasible. A modern aluminum rectangular bar insulated from core by a resin layer is shown in [Figure 16.9](#). For a rectangular bar, the expression of K_R (when skin effect is notable) is (16.10).

$$K_R \approx \beta_{skin} h_r \quad (16.54)$$

with

$$\beta_{\text{skin}} = \sqrt{\frac{\pi f_1 \mu_0}{\rho_{\text{Al}}}} = \sqrt{\frac{\pi \cdot 60 \cdot 1.256 \cdot 10^{-6}}{3.1 \cdot 10^{-8}}} = 87 \text{m}^{-1} \quad (16.55)$$

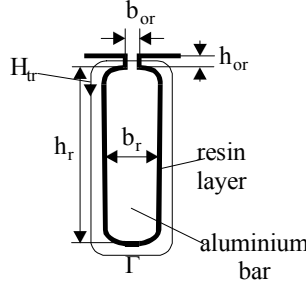


Figure 16.9 Insulated aluminum bar

The rotor bar height h_r is

$$h_r = \frac{3.449}{87} = 3.964 \cdot 10^{-2} \text{m}$$

From (16.11),

$$\frac{(L_{rl})_{S=1}}{(L_{rl})_{S=S_n}} \frac{0.75}{0.85} = \frac{\frac{h_r}{3b_r} K_x + \frac{h_{or}}{b'_{or}}}{\frac{h_r}{3b_r} + \frac{h_{or}}{b_{or}}} = 0.5878 \cdot \frac{0.75}{0.85} = 0.51864 \quad (16.56)$$

$$K_x \approx \frac{3}{2\beta_{\text{skin}} h_r} = \frac{3}{2 \cdot 87 \cdot 3.977 \cdot 10^{-2}} = 0.43 \quad (16.57)$$

We have to choose the rotor slot neck $h_{or} = 1.0 \cdot 10^{-3} \text{m}$ for mechanical reasons. A value of b_{or} has to be chosen, say, $b_{or} = 2 \cdot 10^{-3} \text{m}$. Now we have to check the saturation of the slot neck at start which modifies b_{or} into b_{or}' in (16.56). We use the approximate approach developed in Chapter 9 (paragraph 9.8).

First the bar current at start is

$$I_{\text{bstart}} = I_n \left(\frac{I_{\text{start}}}{I_n} \right) \cdot 0.95 \cdot K_{\text{bs}} \quad (16.58)$$

with $N_r = 64$ and straight rotor slots.

K_{bs} is the ratio between the reduced-to-stator and actual bar current.

$$K_{bs} = \frac{2mW_l K_{wl}}{N_r} = \frac{2 \cdot 3 \cdot (12 \cdot 18) \cdot 0.923}{64} = 18.69075 \quad (16.59)$$

$$I_{bstart} = 5.6 \cdot 0.95 \cdot \frac{120}{\sqrt{3}} \cdot 18.69 = 6896.9A \quad (16.60)$$

Making use of Ampere's law in the Γ contour in [Figure 16.9](#) yields

$$\frac{B_{tr}}{\mu_{rel}\mu_0} [\tau_r - b_{or} + b_{os}\mu_{rel}(H_{tr})] = I_{bstart}\sqrt{2} \quad (16.61)$$

Iteratively, making use of the lamination magnetization curve ([Table 15.4](#), Chapter 15), with the rotor slot pitch τ_r ,

$$\tau_r = \frac{\pi(D_{is} - 2g)}{N_r} = \frac{\pi(0.49 - 2 \cdot 1.5 \cdot 10^{-3})}{64} = 23.893 \cdot 10^{-3}m \quad (16.62)$$

the solution of (16.61) is $B_{tr} = 2.29$ T and $\mu_{rel} = 12$!

The new value of slot opening b_{or}' , to account for tooth top saturation at $S = 1$, is

$$b_{or}' = b_{or} + \frac{\tau_r - b_{or}}{\mu_{rel}} = 2 \cdot 10^{-3} + \frac{(23.893 - 2) \cdot 10^{-3}}{12} = 3.8244 \cdot 10^{-3}m \quad (16.63)$$

Now, with $N_r = 64$ slots, the minimum rotor slot pitch (at slot bottom) is

$$\tau_{rmin} = \frac{\pi(D_{is} - 2g - 2h_r)}{N_r} = \frac{\pi(49 - 2 \cdot 1.5 - 2 \cdot 39.64) \cdot 10^{-3}}{64} = 20 \cdot 10^{-3}m \quad (16.64)$$

With the maximum rotor tooth flux density $B_{tmax} = 1.7T$, the maximum slot width b_{rmax} is

$$b_{rmax} = \tau_r - \frac{B_g}{B_{tmax}} \tau_r = 20 \cdot 10^{-3} \left(1 - \frac{0.8}{1.7} \right) = 10.58mm \quad (16.65)$$

The rated bar current density j_{Al} is

$$j_{Al} = \frac{I_b}{h_i b_r} = \frac{K_i I_n K_{bs}}{h_i b_r} = \frac{0.936 \cdot 120 \cdot 18.69}{39.64 \cdot 10 \sqrt{3}} = 3.061A/mm^2 \quad (16.66)$$

$$\text{with} \quad K_i = 0.8 \cos \varphi_n + 0.2 = 0.8 \cdot 0.92 + 0.2 = 0.936 \quad (16.67)$$

We may now verify (16.56).

$$0.51864 \geq \frac{3 \cdot \frac{39.64}{10} \cdot 0.43 + \frac{1}{3.824}}{\frac{39.64}{3 \cdot 10} \cdot 1 + \frac{1}{2}} = \frac{0.829}{1.821} = 0.455!$$

The fact that approximately the large cage dimensions of $h_r = 39.64$ m and $b_r = 10$ mm with $b_{or} = 2$ mm, $h_{or} = 1$ mm fulfilled the starting current, starting and breakdown torques, for a rotor bar rated current density of only 3.06 A/mm^2 , means that the design leaves room for further reduction of slot width.

We may now proceed, based on the rated bar current $I_b = 1213.38 \text{ A}$ (16.66), to the detailed design of the rotor slot (bar), end ring, and rotor back iron.

Then the teeth saturation coefficient K_{st} is calculated. If notably different from the initial value, the stator design may be redone from the beginning until acceptable convergence is obtained. Further on, the magnetization current equivalent circuit parameters, losses, rated efficiency and power factor, rated slip, torque, breakdown torque, starting torque, and starting current are all calculated.

Most of these calculations are to be done with the same expressions as in Chapter 15, which is why we do not repeat them here.

16.5 DOUBLE CAGE ROTOR DESIGN

When a higher starting torque for lower starting current and high efficiency are all required, the double cage rotor comes into play. However, the breakdown torque and the power factor tend to be slightly lower as the rotor cage leakage inductance at load is larger.

The main constraints are

$$\frac{T_{bk}}{T_{en}} = t_{ek} > 2.0$$

$$\frac{I_{LR}}{I_{ln}} < 5.35$$

$$\frac{T_{LR}}{T_{en}} = t_{LR} \geq 1.5$$

Typical geometries of double cage rotors are shown in [Figure 16.10](#).

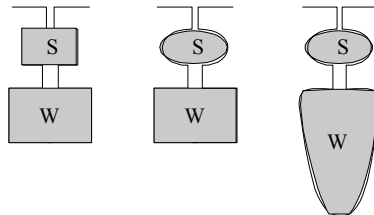


Figure 16.10 Typical rotor slot geometries for double cage rotors.

The upper cage is the starting cage as most of rotor current flows through it at start, mainly because the working cage leakage reactance is high, so its current at primary (f_1) frequency is small.

In contrast, at rated slip ($S < 1.5 \cdot 10^{-2}$), most of the rotor current flows through the working (lower) cage as its resistance is smaller and its reactance is smaller than the starting cage resistance.

In principle, it is fair to say that there is always current in both cages, but, at high rotor frequency ($f_2 = f_1$), the upper cage is more important, while at rated rotor frequency ($f_2 = S_n f_1$), the lower cage takes more current.

The end ring may be common to both cages, but, when frequent starts are considered, separate end rings are preferred because of thermal expansion. (Figure 16.11). It is also possible to make the upper cage of brass and the lower cage of copper.

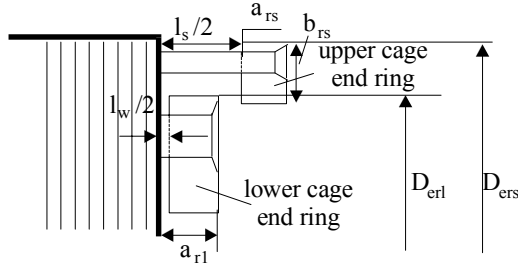


Figure 16.11 Separate end rings

The equivalent circuit for the double cage has been introduced in Chapter 9 (paragraph 9.7) and is inserted here only for easy reference (Figure 16.12).

For the common end ring case, $R_{ring} = R_{ring}$, ring reduced equivalent resistance) reduced to bar resistance), and R_{bs} and R_{bw} are the upper and lower bar resistances.

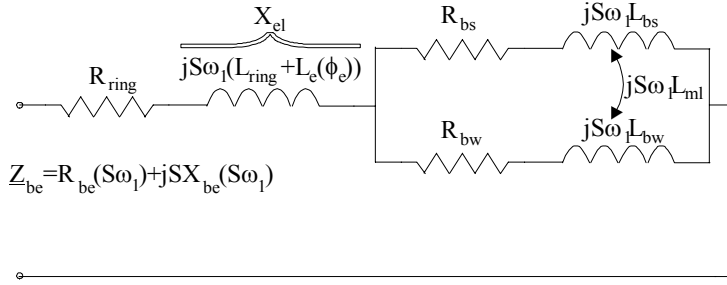
For separate end rings, $R_{ring} = 0$, but the rings resistances $R_{bs} \rightarrow R_{bs} + R_{rings}$, $R_{bw} \rightarrow R_{bw} + R_{ringw}$. L_{lr} contains the differential leakage inductance and only for common end ring, the inductance of the latter.

$$\text{Also,} \quad L_{bs} \approx \mu_0 (l_{geo} + l_s) \frac{h_{rs}}{b_{rs}}; \quad L_e = \mu_0 l_{geo} \frac{h_{or}}{b_{or}} \quad (16.68)$$

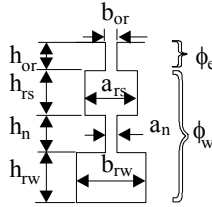
$$L_{bw} \approx \mu_0 (l_{geo} + l_w) \left(\frac{h_{rw}}{3b_{rw}} + \frac{h_n}{a_n} + \frac{h_{rs}}{b_{rs}} \right)$$

The mutual leakage inductance L_{ml} is

$$L_{ml} \approx \mu_0 l_{geo} \frac{h_{rs}}{2b_{rs}} \quad (16.69)$$



a.)



b.)

Figure 16.12 Equivalent circuit of 16.11 double cage a.) and slot geometrical parameters b.)

In reality, instead of l_{geo} , we should use l_i but in this case the leakage inductance of the rotor bar field in the radial channels is to be considered. The two phenomena are lumped into l_{geo} (the geometrical stack length).

The lengths of bars outside the stack are l_s and l_w , respectively.

First we approach the starting cage, made of brass (in our case) with a resistivity $\rho_{brass} = 4\rho_{Co} = 4 \cdot 2.19 \cdot 10^{-8} = 8.76 \cdot 10^{-8} \text{ } (\Omega m)$. We do this based on the fact that, at start, only the starting (upper) cage works.

$$T_{LR} = t_{LR} T_{cn} \approx t_{LR} \frac{P_n}{\omega_1} p_1 = 3 \frac{p_1}{\omega_1} (R_r)_{S=1} 0.95 I_{LR}^2 \quad (16.70)$$

$$I_{LR} = 5.35 I_{ln} = 5.35 \frac{120}{\sqrt{3}} = 371.1 A \quad (16.71)$$

From (16.70), the rotor resistance $((R_r)_{S=1})$ is

$$(R_r)_{S=1} = \frac{t_{LR} P_n}{3 \cdot 0.95 \cdot I_{LR}^2} = \frac{1.5 \cdot 736 \cdot 10^3}{3 \cdot 0.95 \cdot 371.1^2} = 2.8128 \Omega \quad (16.72)$$

From the equivalent circuit at start, the rotor leakage inductance at $S = 1$, $(L_{rl})_{S=1}$, is

$$\begin{aligned} (L_{rl})_{S=1} &= \left(\sqrt{\left(\frac{V_{ph}}{I_{LR}} \right)^2 - (R_s + (R_r)_{S=1})^2 - L_{ls}} \right) \frac{1}{\omega_1} = \\ &= \left(\sqrt{\left(\frac{4000}{371.1} \right)^2 - (0.8576 + 2.8128)^2 - 6.667} \right) \cdot \frac{1}{2\pi 120} = 9.174 \cdot 10^{-3} \text{ H} \end{aligned} \quad (16.73)$$

If it were not for the rotor working cage large leakage in parallel with the starting cage, $(R_r)_{S=1}$ and $(L_{rl})_{S=1}$ would simply refer to the starting cage whose design would then be straightforward.

To produce realistic results, we first have to design the working cage.

To do so we again assume that the copper working cage (made of copper) resistance referred to the stator is

$$(R_r)_{S=S_n} = 0.8R_s = 0.8 \cdot 0.8576 = 0.6861 \Omega \quad (16.74)$$

This is the same as for the aluminum deep bar cage, though it is copper this time. The reason is to limit the slot area and depth in the rotor.

➤ Working cage sizing

The working cage bar approximate resistance R_{be} is

$$R_{be} \approx (0.7 \div 0.8) \frac{(R_r)_{S=S_n}}{K_{bs}} = \frac{0.75 \cdot 0.6861}{7452.67} = 0.6905 \cdot 10^{-4} \Omega \quad (16.75)$$

From (16.6), K_{bs} is

$$K_{bs} = \frac{4m(W_l K_{wl})^2}{N_r} = \frac{4 \cdot 3 \cdot (12 \cdot 18 \cdot 0.923)^2}{64} = 7452.67 \quad (16.76)$$

The working cage cross section A_{bw} is

$$A_{bw} = \frac{\rho_{Co}(l_{geo} + l_w)}{R_{bl}} = 2.2 \cdot 10^{-8} \frac{0.451 + 0.01}{0.6905 \cdot 10^{-4}} = 1.468 \cdot 10^{-4} \text{ m}^2 \quad (16.77)$$

The rated bar current I_b (already calculated from (16.66)) is $I_b = 1213.38 \text{ A}$ and thus the rated current density in the copper bar j_{Cob} is

$$j_{Cob} = \frac{I_b}{A_{bw}} = \frac{1213.38}{1.468 \cdot 10^{-4}} = 8.26 \cdot 10^6 \text{ A/m}^2 \quad (16.78)$$

This is a value close to the maximum value acceptable for radial-axial air cooling.

A profiled bar $1 \cdot 10^{-2}$ m wide and $1.5 \cdot 10^{-2}$ m high is used: $b_{rl} = 1 \cdot 10^{-2}$ m, $h_{rl} = 1.5 \cdot 10^{-2}$ m.

The current density in the end ring has to be smaller than in the bar (about 0.7 to 0.8) and thus the working (copper) end ring cross section A_{rw} is

$$A_{rw} = \frac{A_{bw}}{0.75 \cdot 2 \sin \frac{\pi p_1}{N_r}} = \frac{1.468 \cdot 10^{-4}}{1.5 \sin \frac{\pi \cdot 2}{64}} = 9.9864 \cdot 10^{-4} \text{ m}^2 \quad (16.79)$$

So (from [Figure 16.11](#)),

$$a_{rl} b_{rl} = 9.9846 \cdot 10^{-4} \text{ m}^2 \quad (16.80)$$

We may choose $a_{rl} = 2 \cdot 10^{-2}$ m and $b_{rl} = 5.0 \cdot 10^{-2}$ m.

The slot neck dimensions will be considered as ([Figure 16.12b](#)) $b_{or} = 2.5 \cdot 10^{-3}$ m, $h_{or} = 3.2 \cdot 10^{-3}$ m (larger than in the former case) as we can afford a large slot neck permeance coefficient h_{or}/b_{or} because the working cage slot leakage permeance coefficient is already large.

Even for the deep cage, we could afford a larger h_{or} and b_{or} to stand the large mechanical centrifugal stresses occurring during full operation speed.

We go on to calculate the rotor differential geometrical permeance coefficient for the cage (Chapter 15, Equation (15.82)).

$$\lambda_{dr} = \frac{0.9 \cdot \tau_r \gamma_{dr}}{K_c g} \left(\frac{N_r}{6p_1} \right)^2; \quad \gamma_{dr} = 9 \left(\frac{6p_1}{N_r} \right)^2 \cdot 10^{-2} \quad (16.81)$$

$$\gamma_{dr} = 9 \left(\frac{6 \cdot 2}{64} \right)^2 \cdot 10^{-2} = 0.3164 \cdot 10^{-2}$$

$$\lambda_{dr} = \frac{0.9 \cdot 23.893}{1.5 \cdot 1.5} \cdot 0.3164 \cdot 10^{-2} \cdot \left(\frac{64}{12} \right)^2 = 0.86$$

The saturated value of λ_{drl} takes into account the influence of tooth saturation coefficient K_{st} assumed to be $K_{st} = 0.25$.

$$\lambda_{drs} = \frac{\lambda_{drl}}{1 + K_{st}} = \frac{0.96}{1.25} = 0.688 \quad (16.82)$$

The working end ring specific geometric permeance λ_{erl} is (Chapter 15, Equation (15.83)):

$$\lambda_{erl} = \frac{2.3 \cdot (D_{erl} - b_{rl})}{N_r l_{geo} 4 \sin^2 \left(\frac{\pi p_1}{N_r} \right)} \log \frac{4.7 \cdot (D_{erl} - b_{rl})}{b_{rl} + 2a_{rl}} \approx$$

$$\approx \frac{2.3 \cdot (0.49 - 3 \cdot 10^{-3} - 4 \cdot 10^{-2} - 5 \cdot 10^{-2})}{64 \cdot 0.451 \cdot 4 \sin^2 \left(\frac{\pi \cdot 2}{64} \right)} \log \frac{397 \cdot 10^{-3}}{(50 + 2 \cdot 20) \cdot 10^{-3}} = 0.530 \quad (16.83)$$

The working end ring leakage reactance X_{rlel} (Chapter 11, Equation 15.85) is

$$X_{rlel} \approx \mu_0 2\pi f l_{geo} \cdot 10^{-8} \lambda_{erl} = 7.85 \cdot 60 \cdot 0.451 \cdot 10^{-6} \cdot 0.530 = 1.1258 \cdot 10^{-4} \Omega \quad (16.84)$$

The common reactance (Figure 16.12) is made of the differential and slot neck components, unsaturated, for rated conditions and saturated at $S = 1$.

$$\begin{aligned} (X_{el})_{S=S_n} &= 2\pi f l_{geo} \mu_0 \left(\lambda_{dr} + \frac{h_{or}}{b_{or}} \right) \\ (X_{el})_{S=1} &= 2\pi f l_{geo} \mu_0 \left(\lambda_{drs} + \frac{h_{or}}{b'_{or}} \right) \end{aligned} \quad (16.85)$$

The influence of tooth top saturation at start is considered as before (in 16.60–16.63).

With $b_{or}' = 1.4b_{or}$ we obtain

$$(X_{el})_{S=S_n} = 2\pi 60 \cdot 0.451 \cdot \left(0.86 + \frac{2.5}{3.2} \right) \cdot 1.256 \cdot 10^{-6} = 3.503 \cdot 10^{-4} \Omega$$

$$(X_{el})_{S=1} = 2\pi 60 \cdot 0.451 \cdot \left(0.688 + \frac{2.5}{3.5 + 1.4} \right) \cdot 1.256 \cdot 10^{-6} = 2.6595 \cdot 10^{-4} \Omega$$

Now we may calculate the approximate values of starting cage resistance from the value of the equivalent resistance at start R_{start} .

$$R_{start} = \frac{(R_r)_{S=1}}{K_{bs}} = \frac{2.8128}{7452.67} = 3.7742 \cdot 10^{-4} \Omega \quad (16.86)$$

From Figure 16.12a, the starting cage resistance R_{bes} is

$$R_{bes} \approx \frac{R_{start}^2 + X_{el}^2}{R_{start}} = \frac{(3.7742 \cdot 10^{-4})^2 + (2.6595 \cdot 10^{-4})^2}{(3.7742 \cdot 10^{-4})} = 5.648 \cdot 10^{-4} \Omega \quad (16.87)$$

For the working cage reactance X_{rlw} , we also have

$$X_{rlw} = \frac{R_{start}^2 + X_{el}^2}{X_{el}} = \frac{(3.7742 \cdot 10^{-4})^2 + (2.6595 \cdot 10^{-4})^2}{2.6595 \cdot 10^{-4}} = 8.0156 \cdot 10^{-4} \Omega \quad (16.88)$$

The presence of common leakage X_{el} makes starting cage resistance R_{bes} larger than the equivalent starting resistance R_{start} . The difference is notable and affects the sizing of the starting cage.

The value of R_{bs} includes the influence of starting end ring. The starting cage bar resistance R_b is approximately

$$R_{bs} \approx R_{bes} (0.9 \div 0.95) = 0.9 \cdot 5.648 \cdot 10^{-4} = 5.083 \cdot 10^{-4} \Omega \quad (16.89)$$

The cross section of the starting bar A_{bs} is

$$A_{bs} = \rho_{brass} \frac{l_{geo} + l_s}{R_{bs}} = 4 \cdot 2.2 \cdot 10^{-8} \frac{0.451 + 0.05}{5.083 \cdot 10^{-4}} = 0.8673 \cdot 10^{-4} m^2 \quad (16.90)$$

The utilization of brass has reduced drastically the starting cage bar cross section. The length of starting cage bar was prolonged by $l_s = 5 \cdot 10^{-2} m$ (Figure 16.11) as only the working end ring axial length $a_{rl} = 2 \cdot 10^{-2} m$ on each side of the stack.

We may adopt a rectangular bar again (Figure 16.12b) with $b_{rs} = 1 \cdot 10^{-2} m$ and $h_{rs} = 0.86 \cdot 10^{-2} m$.

The end ring cross section A_{rs} (as in 16.79) is

$$A_{rs} = \frac{A_{bs}}{0.75 \cdot 2 \cdot \sin \frac{\pi p_1}{N_r}} = \frac{0.8673 \cdot 10^{-4}}{1.5 \cdot \sin \frac{\pi \cdot 2}{64}} = 5.898 \cdot 10^{-4} \Omega \quad (16.91)$$

The dimensions of the starting cage ring are chosen to be $a_{rs} \times b_{rs}$ (Figure 16.11) $= 2 \cdot 10^{-2} \times 3 \cdot 10^{-2} m \times m$.

Now we may calculate more precisely the starting cage bar equivalent resistance, R_{bes} , and of the working cage, R_{bew} ,

$$R_{bes} = R_{bs} + \frac{R_{rs}}{2 \sin^2 \left(\frac{\pi p_1}{N_r} \right)}; \quad R_{rs} = \rho_{brass} \frac{l_{rs}}{A_{rs}} \quad (16.92)$$

$$R_{bew} = R_{bw} + \frac{R_{rw}}{2 \sin^2 \left(\frac{\pi p_1}{N_r} \right)}; \quad R_{rw} = \rho_{Co} \frac{l_{rw}}{A_{rw}}$$

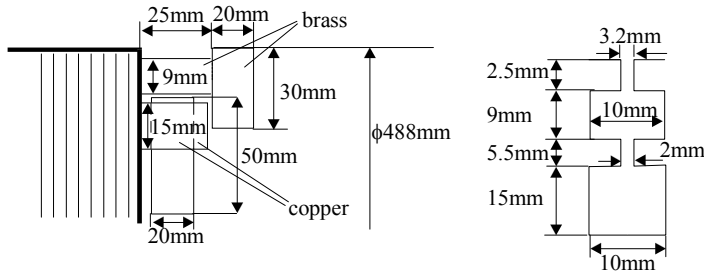


Figure 16.13 Double cage design geometry

The size of end rings is visible in [Figure 16.13](#).

It is now straightforward to calculate R_{bes} and R_{bew} based on l_{rs} and l_{rw} , the end ring segments length of [Figure 16.13](#).

The only unknowns are ([Figure 16.13](#)) the stator middle neck dimensions a_n and h_n .

From the value of the working cage reactance (X_{rlw} 16.88), if we subtract the working end ring reactance (X_{rlel} , (16.84)), we are left with the working cage slot reactance in rotor terms.

$$\begin{aligned} X_{rlslot} &= X_{rlw} - X_{rlel} \\ X_{rlslot} &= 8.0156 \cdot 10^{-4} - 1.1258 \cdot 10^{-4} = 6.8898 \cdot 10^{-4} \Omega \end{aligned} \quad (16.93)$$

The slot geometrical permeance coefficient for the working cage λ_{rl} is

$$\lambda_{rl} = \frac{X_{rlslot}}{2\pi f_i \mu_o l_{geo}} = \frac{6.8898 \cdot 10^{-4}}{2\pi 60 \cdot 1.256 \cdot 10^{-6} \cdot 0.451} = 3.243! \quad (16.94)$$

Let us remember that λ_{rl} refers only to working cage body and the middle neck interval,

$$\lambda_{rl} = \frac{h_{rw}}{3b_{rw}} + \frac{h_n}{a_n} \quad (16.95)$$

Finally,

$$\frac{h_n}{a_n} = 3.243 - \frac{15}{3 \cdot 10} = 2.7435$$

with $a_n = 2 \cdot 10^{-3} \text{ m}$; $h_n = 5.5 \cdot 10^{-3} \text{ m}$

As for the deep bar cage rotor, we were able to determine all working and starting cage dimensions so that they meet (approximately) the starting torque, current, and rated efficiency and power factor.

Again, all the parameters and performance may now be calculated as for the design in Chapter 15.

Such an approach would drastically reduce the number of design iterations until satisfactory performance is obtained with all constraints observed. Also having a workable initial sizing helps to approach the optimization design stage if so desired.

16.6 WOUND ROTOR DESIGN

For the stator, as in previous paragraphs, we approach the wound rotor design methodology. The rotor winding has diametrical coils and is placed in two layers. As the stator slots are open, to limit the airgap flux pulsations (and, consequently, the tooth flux pulsation additional core losses), the rotor slots are to be half – open ([Figure 16.13](#)). This leads to the solution with wave-shape

half-preformed coils made of a single bar with one or more (2) conductors in parallel.

The half-shaped uniform coils are introduced frontally in the slots and then the coil unfinished terminals are bent as required to form wave coils which suppose less material to connect the coils into phases.

The number of rotor slots N_r is now chosen to yield an integer q_2 (slots/pole/pitch) which is smaller than $q_1 = 6$. Choosing $q_2 = 5$ the value of N_r is

$$N_r = 2p_1mq_2 = 2 \cdot 2 \cdot 3 \cdot 5 = 60 \quad (16.96)$$

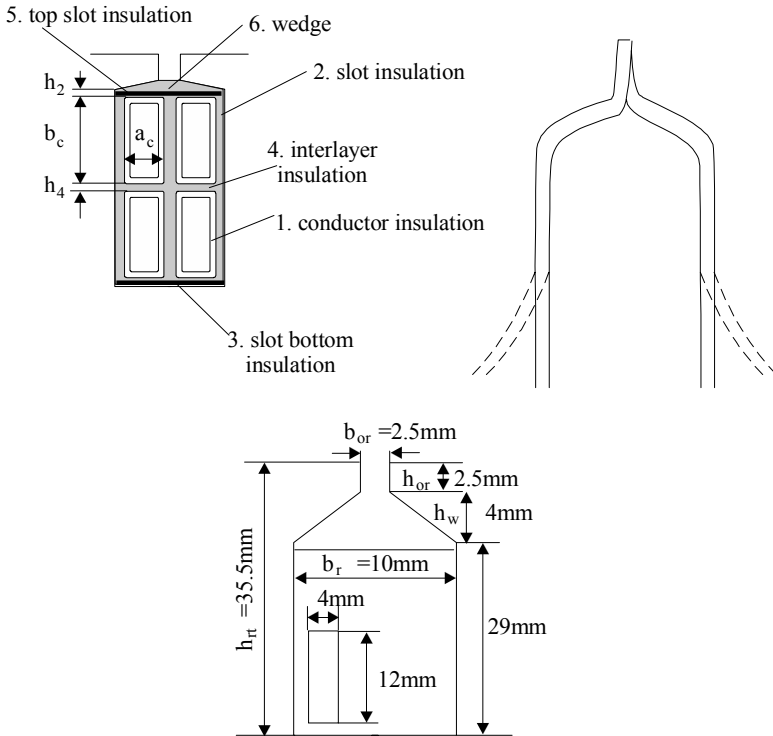


Figure 16.14 Wound rotor slot a.) and half-preformed wave coils b.)

A smaller number of rotor than stator slots leads to low tooth flux pulsation core losses (Chapter 11).

The winding factor is now the distribution factor.

$$K_{w2} = \frac{\sin \frac{\pi}{6}}{q_2 \sin \frac{\pi}{6q_2}} = \frac{0.5}{5 \sin \left(\frac{\pi}{6.5} \right)} = 0.9566 \quad (16.97)$$

The rotor tooth pitch τ_r is

$$\tau_r = \frac{\pi(D_{is} - 2g)}{N_r} = \frac{\pi(490 - 2 \cdot 1.5) \cdot 10^{-3}}{60} = 25.486 \cdot 10^{-3} \text{ m} \quad (16.98)$$

As we decided to use wave coils, there will be one conductor (turn) per coil and thus $n_r = 2$ conductor/slot.

The number of turns per phase, with a_2 current path in parallel, W_2 , is ($a_2 = 1$).

$$W_2 = \frac{N_r n_r}{2m_1 a_2} = \frac{60 \cdot 2}{2 \cdot 3 \cdot 1} = 20 \text{ turns / phase} \quad (16.99)$$

It is now straightforward to calculate the emf E_2 in the rotor phase based on $E_1 = K_E V_{ph}$ in the stator.

$$E_2 = E_1 \frac{W_2 K_{w2}}{W_1 K_{w1}} = \frac{0.97 \cdot 4000 \cdot 20 \cdot 0.9566}{12 \cdot 18 \cdot 0.923} = 372.33 \text{ V} \quad (16.100)$$

Usually star connection is used and the inter – slip – ring (line) rotor voltage V_{2l} is

$$V_{2l} = E_2 \sqrt{3} = 372.33 \cdot 1.73 = 644 \text{ V} \quad (16.101)$$

We know by now the rated stator current and the ratio $K_i = 0.936$ (for $\cos \varphi_n = 0.92$) between rotor and stator current (16.67). Consequently, the rotor phase rated current is

$$I_{2n} = K_i \frac{W_1 K_{w1}}{W_2 K_{w2}} I_n = 0.936 \cdot \frac{12 \cdot 18 \cdot 0.923}{20 \cdot 0.9566} \cdot \frac{120}{\sqrt{3}} = 676.56 \text{ A} \quad (16.102)$$

The conductor area may be calculated after adopting the rated current density: $j_{Cor} > j_{Co} = 6.3 \text{ A/mm}^2$, by (5 to 15)%. We choose here $j_{Cor} = 7 \text{ A/mm}^2$.

$$A_{Cor} = \frac{I_{2n}}{j_{Cor}} = \frac{676.56}{7 \cdot 10^6} = 96.65 \cdot 10^{-6} \text{ m}^2 \quad (16.103)$$

As the voltage is rather low, the conductor insulation is only 0.5 mm thick/side; also 0.5mm foil slot insulation per side and an interlayer foil insulation of 0.5mm suffice.

The slot width b_r is

$$b_r < \tau_r \left(1 - \frac{B_g}{B_{tr \min}} \right) = 28.486 \cdot 10^{-3} \left(1 - \frac{0.8}{1.4} \right) = 10.92 \cdot 10^{-3} \text{ m} \quad (16.104)$$

Let us choose $b_r = 10.5 \cdot 10^{-3} \text{ m}$.

Considering the insulation system width (both side) of $2 \cdot 10^{-3} \text{m}$, the conductor allowable width $a_{\text{cond}} \approx 8.5 \cdot 10^{-3} \text{m}$. We choose 2 vertical conductors, so each has $a_c = 4 \cdot 10^{-3} \text{m}$. The conductors height b_c is

$$b_c = \frac{A_{\text{Cor}}}{2a_c} = \frac{96.65}{2 \cdot 4.0} \approx 12 \cdot 10^{-3} \text{m} \quad (16.105)$$

Rectangular conductor cross sections are standardized, so the sizes found in (16.105) may have to be slightly corrected.

Now the final slot size (with $1.5 \cdot 10^{-3} \text{m}$ total insulation height (radially)) is shown in [Figure 16.14](#).

The maximum rotor tooth flux density B_{trmax} is

$$\begin{aligned} B_{\text{trmax}} &= ((\pi(D_{\text{is}} - 2g) - 2h_{\text{rt}}) - N_r b_r)^{-1} \pi(D_{\text{is}} - 2g) B_g = \\ &= \frac{\pi(490 - 3) \cdot 0.8}{[\pi(490 - 3 - 2 \cdot 35.5) - 60 \cdot 10.5]} = 1.809 \text{T} \end{aligned} \quad (16.106)$$

This is still an acceptable value.

The key aspects of wound rotor designs are now solved. However, this time we will pursue the whole design methodology to prove the performance.

For the wound rotor, the efficiency, power factor ($\cos \phi_n = 0.92$), and the breakdown p.u. torque $t_{\text{bk}} = 2.5$ (in our case) are the key performance parameters.

The starting performance is not so important as such a machine is to use either power electronics or a variable resistance for limited range speed control and for starting, respectively.

➤ The rotor back iron height

The rotor back iron has to flow half the pole flux, for a given maximum flux density $B_{\text{cr}} = (1.4 - 1.6) \text{T}$. The back core height h_{cr} is

$$h_{\text{cr}} = \frac{B_g}{B_{\text{cr}}} \frac{\tau}{\pi} \frac{1}{K_{\text{Fe}}} = \frac{0.8}{1.4} \frac{0.314}{\pi \cdot 0.98} = 0.06 \text{m} \quad (16.107)$$

The interior lamination diameter D_{ir} is

$$D_{\text{ir}} = D_{\text{is}} - 2g - 2h_{\text{rt}} - 2h_{\text{cs}} = (490 - 3 - 71 - 120) \cdot 10^{-3} = 0.296 \text{m} \quad (16.108)$$

So there is enough radial room to accommodate the shaft.

16.7 IM WITH WOUND ROTOR-PERFORMANCE COMPUTATION

We start the performance computation with the magnetization current to continue with wound rotor parameters (stator parameters have already been computed); then losses and efficiency follow. The computation ends with the no-load current, short-circuit current, breakdown slip, rated slip, breakdown p.u. torque, and rated power factor.

➤ Magnetization mmfs

The mmf per pole contains the airgap F_g stator and rotor teeth (F_{st} , F_{rt}) stator and rotor back core (F_{cs} , F_{cr}) components (Figure 16.15).

$$F_{lm} = F_g + (F_{st} + F_{rt}) + \frac{F_{cs}}{2} + \frac{F_{cr}}{2} \quad (16.109)$$

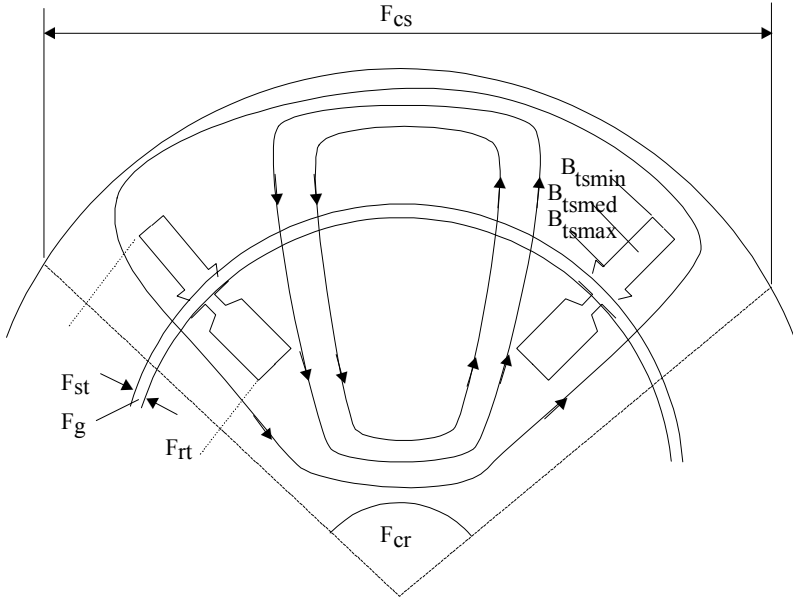


Figure 16.15 Main flux path and its mmfs

➤ The airgap F_g is

$$F_g = K_c g \frac{B_g}{\mu_0} \quad (16.110)$$

The Carter's coefficient K_c is

$$K_c = K_{c1} K_{c2}$$

with

$$K_{c1,2} = \frac{\tau_{s,r}}{\tau_{s,r} - \gamma_{s,r} g}; \quad \gamma_{s,r} = \frac{\left(\frac{b_{os,r}}{g} \right)^2}{5 + \frac{b_{os,r}}{g}} \quad (16.111)$$

K_{c1} has been already calculated in (16.43): $K_{c1} = 1.365$. With $b_{or} = 5 \cdot 10^{-3} \text{ m}$, $g = 1.5 \cdot 10^{-3} \text{ m}$ and $\tau_r = 25.486 \cdot 10^{-3} \text{ m}$, γ_r is

$$\gamma_r = \frac{\left(\frac{5}{1.5}\right)^2}{5 + \frac{5}{1.5}} = 1.333; \quad K_{c2} = \frac{25.486}{25.486 - 1.5 \cdot 1.333} = 1.0852$$

Finally, $K_c = 1.365 \cdot 1.0852 = 1.4812 < 1.5$ (as assigned from start).
From (16.110), F_g is

$$F_g = 1.485 \cdot 1.5 \cdot 10^{-3} \frac{0.8}{1.256 \cdot 10^{-6}} = 1418 \text{ At turns}$$

➤ The stator teeth mmf

With the maximum stator tooth flux density $B_{t\max} = 1.5 \text{ T}$, the minimum value $B_{t\min}$ occurs at slot bottom diameter.

$$\begin{aligned} B_{t\min} &= (\pi(D_{is} + 2h_{st}) - N_s b_s)^{-1} B_g \pi D_{is} = \\ &= (\pi(490 + 2 \cdot 57.2) - 72 \cdot 10)^{-1} \pi \cdot 490 \cdot 0.8 = 1.045 \text{ T} \end{aligned} \quad (16.112)$$

The average value $B_{t\text{med}}$ is

$$B_{t\text{med}} = \frac{\pi D_{is} B_g}{\pi(D_{is} + h_{st}) - N_s b_s} = \frac{\pi \cdot 490 \cdot 0.8}{\pi(490 + 57.2) - 72 \cdot 10} = 1.233 \text{ T} \quad (16.113)$$

For the three flux densities, the lamination magnetization curve (Chapter 15, Table 15.4) yields:

$$H_{ts\max} = 1340 \text{ A/m}, \quad H_{t\text{med}} = 400 \text{ A/m}, \quad H_{t\min} = 230 \text{ A/m}$$

The average value of H_{ts} is

$$\begin{aligned} H_{ts} &= \frac{1}{6}(H_{ts\max} + 4H_{t\text{med}} + H_{t\min}) = \\ &= \frac{1}{6}(1340 + 4 \cdot 400 + 230) = 528.33 \text{ A/m} \end{aligned} \quad (16.114)$$

The stator tooth mmf F_{ts} becomes

$$F_{ts} = h_{st} H_{ts} = 57.2 \cdot 10^{-3} \cdot 528.33 = 30.22 \text{ At turns} \quad (16.115)$$

➤ Rotor tooth mmf (F_{tr}) computation

It is done as for the stator. The average tooth flux density $B_{tr\text{med}}$ is

$$B_{tr\text{med}} = \frac{\pi D_{is} B_g}{\pi(D_{is} - h_{tr}) - N_r b_r} = \frac{\pi \cdot 490 \cdot 0.8}{\pi(490 - 35.52) - 60 \cdot 10.5} = 1.544 \text{ T} \quad (16.116)$$

The corresponding iron magnetic fields for $B_{tr\min} = 1.4 \text{ T}$, $B_{tr\text{med}} = 1.544 \text{ T}$, $B_{tr\max} = 1.809 \text{ T}$ are

$$H_{tr \max} = 9000 \text{ A/m}, \quad H_{tr \text{ med}} = 1750 \text{ A/m}, \quad H_{tr \min} = 760 \text{ A/m}$$

The average H_{tr} is

$$H_{tr} = \frac{1}{6}(H_{tr \max} + 4H_{tr \text{ med}} + H_{tr \min}) = 2793.33 \text{ A/m} \quad (16.117)$$

The rotor tooth mmf F_{tr} is

$$F_{tr} = h_{rt} H_{tr} = 35.5 \cdot 10^{-3} \cdot 2793.33 = 99.163 \text{ Aturns} \quad (16.118)$$

We are now in position to check the teeth saturation coefficient $1 + K_{st}$ (adopted as 1.25).

$$1 + K_{st} = 1 + \frac{F_{ts} + F_{tr}}{F_g} = 1 + \frac{30.22 + 99.163}{1418} = 1.0912 < 1.25 \quad (16.119)$$

The value of $1 + K_{st}$ is a bit too small indicating that the stator teeth are weakly saturated. Slightly wider stator slots would have been feasible. In case the stator winding losses are much larger than stator core loss, increasing the slot width by 10% could lead to higher conductor loss reduction. Another way to take advantage of this situation is illustrated in [4], [Figure 16.16](#), where axial cooling is used, and the rotor diameter is reduced due to better cooling. For a 2 pole 1.9 MW, 6600 V machine, the reduction of mechanical loss led to 1% efficiency rise (from 0.96 to 0.97).

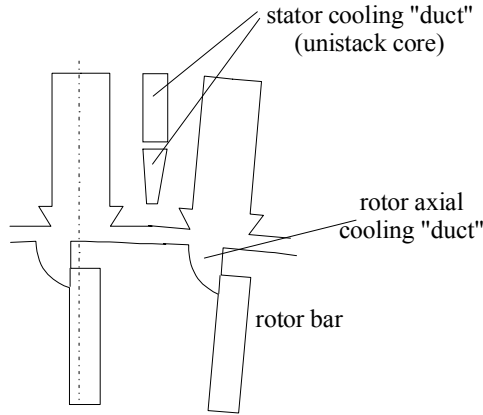


Figure 16.16 Tooth axial ventilation design (high voltage stator)

The stator back core mmf F_{cs} is

$$F_{cs} = l_{cs} H_{cs} \quad (16.120)$$

As both B_{cs} and l_{cs} vary within a pole pitch span, a correct value of F_{cs} would warrant FEM calculations.

We take here $B_{cs\max}$ as reference.

$$B_{cs\max} = \frac{B_g \tau}{\pi h_{cs}} = \frac{0.8 \cdot 0.314}{\pi \left(\frac{0.730 - 0.6044}{2} \right)} = 1.274T \quad (16.121)$$

$$H_{cs\max} = 450A/m$$

The mean length l_{cs} is

$$l_{cs} = \frac{\pi(D_{out} - h_{cs})}{2P_1} K_{cs} = \frac{\pi(0.730 - 628 \cdot 10^{-3})}{2 \cdot 2} 0.4 = 0.2095m \quad (16.122)$$

K_{cs} is an amplification factor which essentially depends on the flux density level in the back iron.

Again $B_{cl\max}$ is a bit too small. Consequently, if the temperature rise allows, the outer stator diameter may be reduced, perhaps to 0.700 m (from 0.73 m).

$$F_{cs} = l_{cs} H_{cl\max} = 0.2095 \cdot 450 = 94.27A\text{turns}$$

➤ Rotor back iron mmf F_{cr} (as for the stator)

$$F_{cr} = H_{cr} l_{cr} = 760 \cdot 0.1118 = 84.95A\text{turns} \quad (16.123)$$

$$H_{cr}(B_{cr}) = H_{cr}(1.4) = 760A/m$$

$$l_{cr} = \frac{\pi(D_{ir} + h_{cr})}{2P_1} K_{cr} = \frac{\pi(0.296 - 0.06)}{2 \cdot 2} 0.4 = 0.1118m \quad (16.124)$$

From (16.109), the total mmf per pole F_{lm} is

$$F_{lm} = 1418 + 30.22 + 99.163 + \frac{94.27 + 84.95}{2} = 1636.99$$

The mmf per pole F_{lm} is

$$F_{lm} = \frac{3\sqrt{2} I_\mu W_l K_{wl}}{p_l \pi} \quad (16.125)$$

So, the magnetization current I_μ is

$$I_\mu = \frac{F_{lm} p_l \pi}{3\sqrt{2} W_l K_{wl}} = \frac{1636.99 \cdot 2 \cdot \pi}{3\sqrt{2} \cdot 12 \cdot 18 \cdot 0.923} = 12.19A \quad (16.126)$$

The ratio between the magnetization and rated phase current I_μ/I_{ln} ,

$$\frac{I_{\mu}}{I_{ln}} = \frac{12.19}{\frac{120}{\sqrt{3}}} = 0.175$$

Even at this power level and $2p_1 = 4$, because the ratio $\tau/g = 314/1.5 = 209.33$ (pole-pitch/airgap) is rather large and the saturation level is low, the magnetization current is lower than 20% of rated current. The machine has slightly more iron than needed or the airgap may be increased from $1.5 \cdot 10^{-3} \text{ m}$ to $(1.8-2) \cdot 10^{-3} \text{ m}$. As a bonus the additional surface and tooth flux pulsation core losses will be reduced. We may simply reduce the outer diameter to notably saturate the stator back iron. The gain will be less volume and weight.

➤ The rotor winding parameters

As the stator phase resistance R_s and leakage reactance (X_{ls}) have already been computed ($R_s = 0.8576 \Omega$, $X_{ls} = 6.667 \Omega$), only the rotor winding parameters R_r and X_{lr} have to be calculated.

The computation of R_r , X_{lr} requires the calculation of rotor coil end connection length based on its geometry (Figure 16.16).

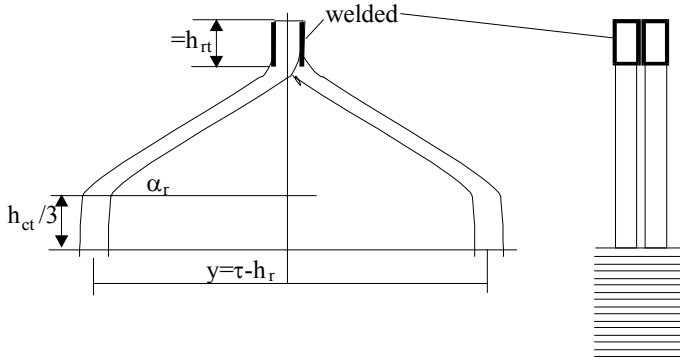


Figure 16.17 Rotor half-formed coil winding

The end connection length l_{fr} per rotor side is

$$l_{fr} = \frac{y}{\cos \alpha_r} + \left(\frac{h_{rt}}{3} + h_{rt} \right) \cdot 2 = \frac{\left(0.314 - 35.5 \cdot 10^{-3} \cdot \frac{\pi}{4} \right)}{0.707} + \left(\frac{4}{3} \cdot 2 \cdot 35.5 \cdot 10^{-3} \right) = 0.5 \text{ m} \quad (16.127)$$

So the phase resistance R_r^r (before reduction to stator) is

$$R_r^r = K_R P_{Co80^0} \frac{2(l_{fr} + l_{geo})W_2}{A_{cor}} = 1.0 \cdot 2.2 \cdot 10^{-8} \frac{2(0.5 + 0.451) \cdot 20}{96 \cdot 10^{-6}} = 0.87 \cdot 10^{-2} \Omega \quad (16.128)$$

➤ The rotor slot leakage geometrical permeance coefficient λ_{sr} is (Figure 16.14)

$$\begin{aligned} \lambda_{s,r} &\approx \frac{2b_c}{3b_r} + \frac{h_4}{4b_r} + \frac{h_2}{b_s} + \frac{h_{or}}{b_{or}} + \frac{2h_w}{(b_{or} + b_r)} = \\ &= \frac{2 \cdot 12}{3 \cdot 10.5} + \frac{1.2}{4 \cdot 10.5} + \frac{1.8}{10.5} + \frac{2.5}{5} + \frac{2 \cdot 4}{5 + 10.5} = 1.978 \end{aligned} \quad (16.129)$$

The rotor differential leakage permeance coefficient λ_{dr} is (as in [3])

$$\lambda_{dr} = \frac{0.9 \cdot \tau_r (q_2 K_{w2})^2 K_{or} \sigma_{dr}}{K_c g} \quad (16.130)$$

$$K_{or} = 1.0 - 0.033 \frac{b_{or}^2}{g \tau_r} = 1.0 - 0.033 \frac{5^2}{1.5 \cdot 24.86} = 0.9778 \quad (16.131)$$

σ_{dr} is the differential leakage coefficient as defined in Figure 6.3, Chapter 6.3. For $q_2 = 5$ and diametrical coils $\sigma_{dr} = 0.64 \cdot 10^{-2}$.

$$\lambda_{dr} = 0.9 \frac{24.86}{1.8 \cdot 1.5} (5 \cdot 0.9566)^2 \cdot 0.9778 \cdot 0.64 \cdot 10^{-2} = 1.4428$$

The end connection permeance coefficient λ_{fr} (defined as for the stator) is

$$\lambda_{fr} = 0.34 \frac{q_2}{l_{fr}} (l_{fr} - 0.64 \tau) = 0.34 \frac{5}{0.406} (0.5 - 0.64 \cdot 0.314) = 1.252 \quad (16.132)$$

The rotor phase leakage reactance X_{lr} (before reduction to stator) is

$$\begin{aligned} X_{lr}^r &= 4\pi f_l \mu_0 W_2^2 \frac{l_i}{P_l q_2} \sum \lambda_{ri} = 4 \cdot 3.14 \cdot 60 \cdot 1.256 \cdot 10^{-6} \cdot \\ &\cdot 20^2 \frac{0.406}{2 \cdot 5} (1.978 + 1.4428 + 1.252) = 7.1828 \cdot 10^{-2} \Omega \end{aligned} \quad (16.133)$$

After reduction to stator, the rotor resistance and leakage reactances R_r^r , X_{lr}^r become R_r and X_{lr} .

$$R_r = R_r \left(\frac{W_1 K_{w1}}{W_2 K_{w2}} \right)^2 = 0.87 \cdot 10^{-2} \cdot \left(\frac{12 \cdot 18 \cdot 0.923}{20 \cdot 0.9566} \right)^2 = 0.94 \Omega \quad (16.134)$$

$$X_{lr} = X_{lr} \left(\frac{W_1 K_{w1}}{W_2 K_{w2}} \right)^2 = 7.1828 \cdot 10^{-2} \cdot 108.6 = 7.8 \Omega$$

The magnetization reactance X_m is

$$X_m \approx \frac{(V_{ph} - I_\mu X_{ls})}{I_\mu} = \frac{(4000 - 12.19 \cdot 6.667)}{12.19} = 326.56 \Omega \quad (16.135)$$

The p.u. parameters are

$$\begin{aligned} X_n &= \frac{V_{ph}}{I_{ln}} = \frac{4000}{\frac{120}{\sqrt{3}}} = 57.66 \Omega \\ r_s &= \frac{R_s}{X_n} = \frac{0.8756}{57.66} = 0.01487 \text{ p.u.} \\ r_r &= \frac{R_r}{X_n} = \frac{0.94}{57.66} = 0.0163 \text{ p.u.} \\ x_{ls} &= \frac{X_{ls}}{X_n} = \frac{6.667}{57.66} = 0.1156 \text{ p.u.} \\ x_{lr} &= \frac{X_{lr}}{X_n} = \frac{7.8}{57.66} = 0.1352 \text{ p.u.} \\ x_m &= \frac{X_m}{X_n} = \frac{326.56}{57.66} = 5.66 \text{ p.u.} > (3.2 - 4) \text{ p.u.} \end{aligned} \quad (16.136)$$

Again, x_m is visibly higher than usual but, as explained earlier, this may be the starting point for further reduction in machine size. A simple reduction of the outer stator diameter to saturate the stator back iron would bring x_m into the standard interval of (3.5 to 4)p.u. for 4 pole IMs of 700 kW power range with wound rotors. An increase in airgap will produce similar effects, with additional core loss reduction.

We leave x_m as it is in (16.136) as a reminder that the IM design is an iterative procedure.

➤ Losses and efficiency

The losses in induction machines may be classified as

- Conductor (winding or electric) losses
- Core losses
- Mechanical / ventilation losses

The stator winding rated losses p_{cos} are

$$p_{\cos} = 3R_s I_{1n}^2 = 3 \cdot 0.8576 \cdot 69.36^2 = 12.378 \cdot 10^3 \text{ W} \quad (16.137)$$

For the rotor winding:

$$p_{\text{cor}} = 3R_r I_{1n}^2 = 3R_r K_i^2 I_{1n}^2 = 3 \cdot 0.94 \cdot (0.936 \cdot 69.36)^2 = 11.885 \cdot 10^3 \text{ W} \quad (16.138)$$

Considering a voltage drop, $V_{ss} = 0.75 \text{ V}$ in the slip ring and brushes, the slip-ring brush losses p_{sr} (with I_{2n} from (16.102)) is

$$p_{sr} = 3V_{sr} I_{2n} = 3 \cdot 0.75 \cdot 676.56 = 1.522 \cdot 10^3 \text{ W}$$

Additional winding losses due to skin and proximity effects (Chapter 11) may be neglected for this case due to proper conductor sizing.

The core losses have three components (Chapter 11).

- Fundamental core losses
- Additional no load core losses
- Additional load (stray) core losses

The fundamental core losses are calculated by empirical formulas (FEM may also be used) as in Chapter 15.

The stator teeth fundamental core losses $P_{\text{iron}t}$ is (15.98)

$$P_{\text{iron}t} = K_t p_{10} \left(\frac{f_1}{50} \right)^{1.3} B_{ts}^{1.7} G_{ts} \quad (16.139)$$

$K_t = 1.6 - 1.8$, takes into account the mechanical machining influence on core losses; $p_{10}(1\text{T}, 50 \text{ Hz}) = (2 - 3) \text{ W/Kg}$ for 0.5 mm thick laminations.

The value of B_t varies along the tooth height so the average value $B_{ts}(H_{ts})$, Equation (16.114) is $B_{ts}(528 \text{ A/m}) = 1.32 \text{ T}$.

The stator teeth weight G_{ts} is

$$\begin{aligned} G_{ts} &= \left[\frac{\pi}{4} \left((D_{is} + 2h_{st})^2 - D_{is}^2 \right) - h_{ts} b_s N_s \right] l_i \gamma_{\text{iron}} = \\ &= \left[\frac{\pi}{4} \left((0.490 + 2 \cdot 56.2 \cdot 10^{-3})^2 - 0.490^2 \right) - 56.2 \cdot 10^{-3} \cdot 10 \cdot 10^{-3} \cdot 72 \right] \cdot \\ &\quad \cdot 0.406 \cdot 7800 = 175.87 \text{ Kg} \end{aligned} \quad (16.140)$$

From (16.139),

$$P_{\text{iron}t} = 1.8 \cdot 2.4 \left(\frac{60}{50} \right)^{1.3} 1.32^{1.7} \cdot 175.87 = 1.543 \cdot 10^3 \text{ W}$$

Similarly, the stator back iron (yoke) fundamental losses p_{iron^y} are

$$P_{\text{iron}^y} = K_y p_{10} \left(\frac{f_1}{50} \right)^{1.3} B_{cs}^{1.7} G_{ys} \quad (16.141)$$

$K_y = 1.2 - 1.4$ takes care of the influence of mechanical machining on yoke losses (for lower power machines this coefficient is larger).

The stator yoke weight G_{ys} is

$$G_{ys} = \frac{\pi(D_{out}^2 - (D_{is} + 2h_{st})^2)}{4} l_i \gamma_{iron} =$$

$$= \frac{\pi}{4} (0.73^2 - (0.49 + 2 \cdot 0.0562)^2) \cdot 0.406 \cdot 7800 = 422.6 \text{ Kg} \quad (16.142)$$

$$P_{iron}^y = 1.35 \cdot 2.4 \cdot \left(\frac{60}{50}\right)^{1.7} \cdot 1.274^{1.7} \cdot 422.6 = 2.619 \cdot 10^3 \text{ W}$$

Stator and rotor surface core losses, due to slot openings (Chapter 11), are approximately

$$P_{siron}^s = 2p_l \tau \frac{(\tau_s - b_{os})}{\tau_s} l_i K_{Fe} p_{siron}^s \quad (16.143)$$

with the specific surface stator core losses (in W/m^2) p_{siron}^s as

$$p_{siron}^s \approx 5 \cdot 10^5 B_{os}^2 \tau_r^2 K_0 \left(\frac{N_r 60 f_l}{p_l \cdot 10000} \right)^{1.5} \quad (16.144)$$

B_{os} is the airgap flux pulsation due to rotor slot openings

$$B_{os} = \beta_s K_c B_g = 0.2 \cdot 1.48 \cdot 0.8 = 0.2368 \text{ T} \quad (16.145)$$

β_s comes from [Figure 5.4](#) (Chapter 5). With $b_{or}/g = 5/1.5$, $\beta_s \approx 0.2$. K_0 again depends on mechanical factors: $K_0 = 1.5$, in general.

$$p_{siron}^s = 5 \cdot 10^5 \cdot 0.2368^2 \cdot 0.02486^2 \cdot 1.5 \cdot \left(\frac{60 \cdot 60 \cdot 60}{2 \cdot 10000} \right)^{1.5} = 922.49 \text{ W/m}^2$$

From (16.143):

$$P_{siron}^s = 2 \cdot 2 \cdot 0.314 \frac{(21.37 - 10)}{21.37} \cdot 0.406 \cdot 0.95 \cdot 922.49 = 0.237 \cdot 10^3 \text{ W}$$

As the rotor slot opening b_{or} per gap g ratio is only $5/1.5$, the stator surface core losses are expected to be small.

The rotor surface core losses p_{siron}^r are

$$P_{siron}^r = 2p_l \frac{(\tau_r - b_{or})}{\tau_s} \tau_l K_{Fe} p_{siron}^r \quad (16.146)$$

with
$$p_{\text{siron}}^r = 5 \cdot 10^5 B_{\text{or}}^2 \tau_s^2 K_0 \left(\frac{N_r 60 f_1}{p_1 \cdot 10000} \right)^{1.5}; \quad B_{\text{or}} = \beta_r K_c B_g$$

The coefficient β_r for $b_{\text{os}}/g = 10/1.5$ is, [Figure 5.4](#), Chapter 5, $\beta_r = 0.36$. Consequently,

$$B_{\text{or}} = 0.36 \cdot 1.48 \cdot 0.8 = 0.426 \text{ T}$$

$$p_{\text{siron}}^r = 5 \cdot 10^5 \cdot 0.426^2 \cdot 0.0214^2 \cdot 2.0 \cdot \left(\frac{72 \cdot 60 \cdot 60}{2 \cdot 10000} \right)^{1.5} = 3877 \text{ W/m}^2$$

$K_0 = 2.0$ as the rotor is machined to a rated airgap.

$$P_{\text{siron}}^r = 2 \cdot 2 \cdot \frac{(24.86 - 5)}{24.86} \cdot 0.314 \cdot 0.406 \cdot 0.95 \cdot 3877 = 1.5 \cdot 10^3 \text{ W}$$

The tooth flux pulsation core losses are still to be determined.

$$P_{\text{puls}} = P_{\text{puls}}^s + P_{\text{puls}}^r \approx K_0' 0.5 \cdot 10^{-4} \left[\left(N_r \frac{f_1}{p_1} \beta_{\text{ps}} \right)^2 G_{\text{ts}} + \left(N_s \frac{f_1}{p_1} \beta_{\text{pr}} \right)^2 G_{\text{tr}} \right] \quad (16.147)$$

$$\beta_{\text{ps}} \approx (K_{c2} - 1) B_g = (1.085 - 1) \cdot 0.8 = 0.068 \text{ T}$$

$$\beta_{\text{pr}} \approx (K_{c1} - 1) B_g = (1.36 - 1) \cdot 0.8 = 0.288 \text{ T}$$

$K_0' > 1$ is a technological factor to account for pulsation loss increases due to saturation and type of lamination manufacturing technology (cold or hot stripping). We take here $K_0' = 2.0$.

The rotor teeth weight G_{tr} is

$$\begin{aligned} G_{\text{tr}} &= \left[\frac{\pi \left((D_{\text{is}} - 2g)^2 - (D_{\text{is}} - 2g - 2h_{\text{rt}})^2 \right)}{4} - N_r h_{\text{rt}} b_r \right] l_i \gamma_{\text{iron}} = \\ &= \left[\frac{\pi \left((0.49 - 3 \cdot 10^{-3})^2 - (0.487 - 2 \cdot 0.0355)^2 \right)}{4} - 60 \cdot 0.0355 \cdot 105 \cdot 10^{-2} \right] \cdot (16.148) \\ &\quad \cdot 0.406 \cdot 7800 = 88.55 \text{ Kg} \end{aligned}$$

From (16.147),

$$\begin{aligned} P_{\text{puls}} &= 2 \cdot 0.5 \cdot 10^{-4} \left[\left(60 \cdot \frac{60}{2} \cdot 0.068 \right)^2 \cdot 175.87 + \left(72 \cdot \frac{60}{2} \cdot 0.288 \right)^2 \cdot 88.55 \right] = \\ &= 3.684 \cdot 10^3 \text{ W} \end{aligned}$$

The mechanical losses due to friction (brush-slip ring friction and ventilator on-shaft mechanical input) depend essentially on the type of cooling machine design.

We take them all here as being $P_{mec} = 0.01P_n$. For two-pole or high-speed machines, $P_{mec} > 0.01P_n$ and should be, in general, calculated as precisely as feasible in a thorough mechano thermal design.

- The machine rated efficiency η_n is

$$\eta_n = \frac{P_n}{P_n + p_{cos} + p_{cor} + P_{sr} + P_{iron}^t + P_{iron}^y + P_{siron}^s + P_{siron}^r + P_{puls} + P_{mec}} =$$

$$= \frac{736 \cdot 10^3}{10^3(736 + 12.378 + 11.885 + 1.522 + 1.543 + 2.619 + 1.237 + 3.877 + 3.684 + 7.36)} =$$

$$= 0.943$$

(16.149)

As the efficiency is notably below the target value (0.96), efforts to improve the design are in order. Additional losses are to be reduced. Here is where optimization comes into play.

- The rated slip S_n (with short-circuited slip rings) may be calculated as

$$S_n \approx \frac{R_r K_r I_{ln}}{K_e V_{ph}} = \frac{0.94 \cdot 0.936 \cdot 69.36}{0.97 \cdot 4000} = 0.0157 \quad (16.150)$$

Alternatively, accounting for all additional core losses in the stator,

$$S_n = \frac{p_{cor}}{P_n + p_{cor} + p_{mec}} = \frac{11.885 \cdot 10^3}{10^3 \cdot (736 + 11.885 + 7.36)} = 0.015736 \quad (16.151)$$

The rather large value of rated slip is an indication of not so high efficiency. A reduction of current density both in the stator and rotor windings seems appropriate to further increase efficiency.

The rather low teeth flux densities allow for slot area increases to cause winding loss reductions.

- The breakdown torque
The approximate formula for breakdown torque is

$$T_{bk} \approx \frac{3p_l}{2\omega_l} \frac{V_{ph}^2}{X_{ls} + X_{lr}} = \frac{3 \cdot 2}{2 \cdot 2\pi \cdot 60} \frac{4000^2}{(6.67 + 7.8)} = 8.8035 \cdot 10^3 \text{ Nm} \quad (16.152)$$

The rated torque T_{en} is

$$T_{en} = \frac{p_l P_n}{\omega_l (1 - S_n)} = \frac{2 \cdot 736 \cdot 10^3}{2\pi \cdot 60 \cdot (1 - 0.015)} = 3.966 \cdot 10^3 \text{ Nm} \quad (16.153)$$

The breakdown torque ratio t_{ek} is

$$t_{ek} = \frac{T_{bk}}{T_{en}} = \frac{8.8035}{3.966} \approx 2.2 \quad (16.154)$$

This is an acceptable value.

16.8 SUMMARY

- Above 100 kW, induction motors are built for low voltage (≤ 690 V) and for high voltage (between 1 KV and 12 KV).
- Low voltage IMs are designed and built with up to more than 2 MW power per unit, in the eventuality of their use with PWM converter supplies.
- For constant voltage and frequency supply, besides efficiency and initial costs, the starting torque and current and the peak torque are important design factors.
- The stator design is based on targeted efficiency and power factor, while rotor design is based on the starting performance and breakdown torque.
- Deep bar cage rotors are used when moderate p.u. starting torque is required ($t_{LR} < 1.2$). The bar depth and width are found based on breakdown torque, starting torque, and starting current constraints. This methodology reduces the number of design iterations drastically.
- The stator design is dependent on the cooling system: axial cooling (unistack core configuration) and radial-axial cooling (multistack core with radial ventilation channels, respectively).
- For high voltage stator design, open rectangular slots with form-wound chorded (rigid) coils, built into two-layer windings, are used. Practically only rectangular cross section conductors are used.
- The rather large total insulation thickness makes the stator slot fill in high voltage windings rather low, with slot aspect ratios $h_{st}/b_s > 5$. Large slot leakage reactance values are expected.
- In low voltage stator designs, above 100 kW, semiclosed-slot, round-wire coils are used only up to 200 to 250 kW for $2p_1 = 2, 4$; also, for larger powers and large numbers of poles ($2p_1 = 8, 10$), with quite a few current paths in parallel ($a_1 = p_1$) may be used.
- For low voltage stator designs, prepared also for PWM converter use, only rectangular shape conductors in form-wound coils are used to secure good insulation as required for PWM converter supplies. To reduce skin effect, the conductors are divided into a few elementary conductors in parallel. Stranding is performed twice, at the end connection zone (Chapter 9).
- Deep bar design handles large breakdown torques, $t_{bk} \approx 2.5 - 2.7$ and more, with moderate starting torques $t_{LR} < 1.2$ and starting currents $i_{LR} < 6.1$. Based on breakdown torque, starting torque, and current values, with stator already designed (and thus stator resistance and leakage reactance values known), the rotor resistance and leakage reactance values (with and without skin effect and leakage saturation consideration) are calculated. This leads

directly to the adequate rectangular deep bar geometry. If other bar geometries are used, the skin effect coefficients are calculated by the refined multilayer approach (Chapter 9).

- Double cage rotor designs lead to lower starting currents $i_{LR} < 5.5$, larger starting torque $t_{LR} > (1.4 \text{ to } 1.5)$ and good efficiency but moderate power factor.

A similar methodology, based on starting and rated power performance leads to the starting and working cage sizing, respectively. Thus, a first solution meeting the main constraints is obtained. Refinements should follow eventually through design optimization methods.

- For limited variable speed range, or for heavy and frequent starting (up to breakdown torque at start), wound rotor designs are used.
- The wound-rotor two-layer windings are, in general, low voltage type, with half-formed diametrical wave uniform coils in semiopen slots (to reduce surface and tooth flux pulsation core loss). A few elementary conductors in parallel may be needed to handle the large rotor currents as the power goes up.
- The wound rotor design is similar to stator design, being based only on rated targeted efficiency (and power factor) and assigned rotor current density.
- This chapter presents design methodologies through a single motor (low and high voltage stator 736 kW (1,000 HP) design with deep bar cage, double cage, and wound rotor, respectively). Only for the wound rotor, all parameters (losses, efficiency, rated slip, breakdown torque, and rated power factor) are calculated. Ways to improve the resultant performance are indicated. Temperature rise calculations may be done as in Chapter 15, but with more complex mathematics. If temperature rise is not met (insulation class F and temperature rise class B) and it is too high, a new design with a larger geometry or lower current densities or (and) flux densities has to be tried.
- Chapters 14, 15, and 16 laid out the machine models (analysis) and a way to size the machine for given performance and constraints (preliminary synthesis).

The mathematics in these chapters may be included in a design optimization software. Design optimization will be treated in a separate chapter. FEM is to be used for refinements.

- The above methodologies are by no means unique to the scope, as IM design is both a science and an art.

16.9 REFERENCES

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